REACTOR CORE MELTDOWN CONTAINMENT FOR OFFSHORE APPLICATIONS

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ABSTRACT

An investigation of the applicability of existing core catcher proposals designed to mitigate the effects of a hypothetical reactor core meltdown on a presently envisioned offshore, barge-mounted nuclear power plant design is undertaken. In addition, a new core catcher concept employing a graphite pebble bed is described and evaluated.

To establish the envelope of design parameter constraints necessary to both devise and evaluate core catcher concepts, a detailed description of the one offshore conceptual design which has progressed far enough to permit specific analysis is presented. Potential plant system interactions with a hypothetical core catcher are identified, and weight, moment, and volume limitations are described. An analytical evaluation of the meltdown sequence, from primary system blowdown to reactor vessel meltthrough, is carried out to develop the quantitative aspects of the accident necessary to assess the viability of the proposed core catcher designs.

It is concluded that none of the previously existing core catcher designs is completely satisfactory for barge use, primarily due to the excessive weight/volume required, metallurgical and thermophysical uncertainties, cost and complexity, and the potential for steam explosions. The graphite pebble bed is shown to circumvent these problems, freezing the debris almost immediately upon its entry into the bed, and providing a grace period of several hours to initiate active cooling, which can be readily effected using a water spray and flood system.

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INTRODUCTION

1.1 FOREWORD

Environmental considerations restricting onshore siting and potential cost reductions due to standardization have induced nuclear reactor manufacturers and utilities to initiate plans for offshore siting of central station units for the generation of electric power. This concept, however, results in a significant departure from the reactor's usual environment. In particular, the presence of the open ocean rather than earth underneath the plant places increased emphasis on the provision of added insurance for retention of core meltdown products in the extremely unlikely event of a severe loss of coolant accident coincident with failure of emergency core cooling. It is the purpose of this thesis to evaluate means for retention of these core meltdown products to prevent breach of the plant's confines and the subsequent release to the environment.

1.2 BACKGROUND

1.2.1 Energy Demand and Siting Problems

The United States is presently doubling her energy demands every ten years, producing over 1.4 trillion kwh of electricity in 1968, almost as much as the next five countries combined (F4). While the population has increased by a factor of five over the last 100 years, the total energy consumed has risen over seventeen-fold over the same period, indicating

a substantial rise in _per capita_ usage.

Given present technology and the rapid depletion of natural resources, the electrical industry has recognized that nuclear power must accept the burden of the increased energy demand. However, last year 20% of the electrical utilities' new plants were delayed by court litigation and 40% by general conservation and environmental hearings (S3). Most of the public concern and ensuing delays are centered around land resource depletion, visual encroachment, thermal pollution, air pollution, and radiation fears. Alternatives to land-based siting have therefore been sought by the electrical industry.

1.2.2 Offshore Concept

In an attempt to ameliorate the environmental impact and public concern attendant with the siting of nuclear power plants in populated areas and to benefit economically from an assembly line production process for the plants, Offshore Power Systems (OPS), a Westinghouse-Tenneco joint enterprise, announced plans in August, 1971, to construct barge-mounted plants which could be towed to areas offshore and moored there, protected by massive breakwaters (E2); General Electric (N6) and Combustion Engineering have since begun extensive investigations of the offshore concept. A year later in September, 1972, the Public Service Electric Gas Company of New Jersey (PSEG) signed a $750 million contract with OPS for construction and delivery of two 1150 megawatt (electrical) nuclear plants to be moored southeast of Little Egg Inlet, 3 miles offshore and 12 miles northeast of

Atlantic City (S5). More recently, OPS has revealed it is negotiating with six other utility groups, all on the east and gulf coasts, believing to have "better than a 50-50 chance" of selling four-six platform mounted units by July, 1973 (N4).

The offshore scheme circumvents or totally obviates many of the existing siting objections listed in Section 1.2.1. Land resources, at a premium near highly populated coastal areas where much of the increased power capacity is required, would not be depleted. By locating far enough at sea, the plants would not impair the inland and coastal beach scenery. The thermal pollution that accompanies many land-based plants in the form of a 10-to 20- degree rise in the temperature of several miles of downstream river water would be reduced; PSEG engineers estimate that the maximum rise in the sea water temperature will be 5 degrees, affecting an area of only 5 acres of ocean surrounding the plant (N1). Radiation hazards associated with normal plant operation would be lowered due to the plant's location several miles away from the populace and by the resulting natural seawater buffer and generally prevailing offshore winds and weather patterns on the east coast.

Several additional monetary incentives are claimed by OPS for its concept (D3). By proceeding with site preparation while the manufacturing of the plant is being carried out at another location, investment time is reduced. Investment time is further lowered by a claimed two year reduction in lead time over conventional nuclear plants. Transmission losses and costs are lowered due to the plant's proximity to the load center.

Because of its standardized design, the effort required for plant review by regulatory agencies should be reduced; indeed, the AEC recently approved "batch licensing" of the offshore plants, requiring only subsequent approval of specific site locations and preparation (N5). Finally, since the shipyard workers will be permanent employees of OPS, as opposed to the transitory labor force currently utilized in terrestrial nuclear construction, construction time and costs should decrease as experience and expertise increase along a learning curve.

However, several postulated accidents are potentially more severe at sea than on land. In particular, a complete core meltdown and subsequent containment melt-through, the so-called "China Syndrome", while to some extent self-sealing on land, could conceivably contaminate several hundred square miles of ocean to a level in excess of the maximum allowable concentration of many nuclides due to wide-range dispersal by currents and tidal motion.

1.2.3 Core Meltdown Accident At Sea

As with conventional terrestrial plants, the offshore concept must satisfy stringent AEC safety requirements prior to being licensed for power operation, in particular demonstrating the capability to prevent core meltdown and the subsequent release of fission products to the ocean environment in the event of the "maximum credible accident", a double-ended pipe break in the primary coolant system. The complete loss of all the core fission products to the sea is not deemed a credible acci-

dent, a series of engineering failures being required to lead to significant core melting. In particular, dual safeguard systems are used to keep the core subcritical and to remove the fission product decay heat. The series probability of failure, first of the primary coolant piping, and then of the safeguards systems is so small as to render the meltdown accident "incredible". However, it is fair to say that some, both within and without the nuclear industry, are not convinced that the Emergency Core Cooling System (ECCS) will function as expected to cool a reactor core following a loss of coolant accident (LOCA) (G3,F3). The probability of occurrence of a complete reactor meltthrough will not be further considered in this thesis; rather, the accident will be assumed to occur and means for mitigating its consequences will be evaluated. NOTHING IN THE THESIS IS TO BE CONSTRUED AS LENDING ANY CREDIBILITY TO THE POSSIBILITY OF A CORE MELTDOWN.

The consequences of a nuclear reactor accident at sea have been investigated by General Dynamics for the Environmental Protection Agency (G1). The plant under consideration in this report was similar to that proposed by Offshore Power Systems (a 3560 thermal megawatt, pressurized water reactor system) but differs in that it was considered to be submerged to a depth of 250 feet. Two proposed accidents were studied for their environmental effects: 1) the double ended primary coolant pipe break, with proper functioning of the Emergency Core Cooling System preventing gross fuel meltdown, and; 2) a breach of containment accident caused by collision with a submarine or

sinking ship, with subsequent primary coolant piping failure due to thermal or mechanical shock. The first accident assumes, as do the safety analyses of all pressurized water plants for site evaluation, 100% of the core's noble gas fission products, 50% of its halogens, and 1% of its solid fission products are released to the containment, of which a subsequent leak rate of 0.1% per day from the containment to the ocean is postulated; however, this leak is assumed to occur for only 15 minutes since the properly functioning containment spray system quickly reduces the containment pressure below sea pressure. The expected total release to the ocean for this hypothetical situation is thus

$$\text{Release} = \text{core total} \times \% \text{ to containment} \times \frac{0.1\%}{\text{day}} \times \frac{15 \text{ min}}{1440 \frac{\text{min}}{\text{day}}} .$$

For the solid fission products, of which 100% of core total could be released in the event of a complete core meltthrough, only $1 \times 10^{-5}\%$ of the core total is assumed released in this accident study. The breach of containment accident assumes a primary coolant activity resulting from operation with 1% of the fuel elements failed, all of which is discharged to the surrounding waters following the accident. Additionally, due to the sudden primary system pressure decrease, all cladding is assumed to become perforated, resulting in a release of all fission product activity contained in the fuel-clad gap. The concentrations of important nuclides resulting from the two accidents discussed were then computed in terms of a ratio of actual concentrations to maximum permissible concentration (MPCC)

of the radionuclide as defined by Ref. B4. The MPCC is determined for a steady state concentration of the nuclide under consideration, which does not exist after the accident, and thus the ratio is conservative for the hypothetical accidents assumed. Additionally, the actual concentration of at least some of the nuclides reaching man is dependent upon the assimilation and elimination rates of various marine organisms; these rates were not considered in determining the concentration ratios in Ref. G1. The results for the two accidents are summarized in Table 1.1. For these accidents it is concluded that harmful effects on marine life are possible for a 10 week period after the accident. As indicated above, this study assumes only a very small percentage of the core's fission product inventory will actually escape into the ocean environment; even a conservative extrapolation of Table 1.1 to a complete core meltdown results in the conclusion that virtually all radionuclides would exceed their permissable concentrations by several orders of magnitude. In view of the potential effects of an at-sea meltthrough, an investigation into means for mitigating the accident and preventing environmental contamination is justified.

1.2.4 Need for, and Current Status of, Core Catcher Design

The official public position taken by the Offshore Power Systems management is that since the probability of occurrence of the meltdown/meltthrough accident is the same as for terrestrial-based plants, OPS should not be monetarily penalized by

Table 1.1

ESTIMATED RELEASE OF SOME IMPORTANT NUCLIDES IN POSTULATED ACCIDENTS

Nuclide	Curies in Core	Curies in Coolant + Gap	CC/MPC* LOCA	CC/MPC BREACH
Co-58	---	1.58	small	4.9
Co-60	---	0.18	NC**	NC
Sr-89	1.43(8)#	2.54	NC	NC
Sr-90	8.23(6)	0.12	NC	NC
I-131	8.78(7)	1.52(7)	5.1(3)	3.4(8)
I-133	2.02(8)	1.53(7)	3.3(3)	9.7(7)
Y-91	1.77(8)	1.06(2)	2.3	13.0
Kr-88	1.08(8)	1.93(6)	gas	gas
Xe-133	2.02(8)	1.99(7)	gas	gas

# denotes powers of 10
* ratio of computed concentration to max permissible concentration
** not computed

having to develop another line of defense to guard against the incident (D4). However, the Advisory Committee on Reactor Safeguards (ACRS) of the AEC stated in November, 1972, that "OPS should give further consideration to possible means for assuring maintenance of containment integrity in the highly unlikely event of core meltthrough, taking into account the possible advantage of the availability of the large source of cold water surrounding the platform" (N2).

An advisory task force appointed by the AEC in 1966 to investigate the adequacy of the safeguards systems to protect against a potential reactor core meltdown and subsequent radioactive release to the environment concluded that at that time, reliable and practical methods of handling large molten masses of fuel did not exist; further, the report pointed out the desirability of improving the independence of the containment system as an engineered safeguard (E3). Another group, convening five years later in 1971, published in BMI-1910 (M3) the most extensive summary and analysis yet available of the ability of present containment structures to sustain a core meltdown (M3); that report concluded that little had been done experimentally or conceptually since the publishing of Ref. E3. Statements made at public environmental hearings indicate that only minimal effort has been made to develop passive or sem-passive safety devices to prevent core meltthrough, primarily because the phenomena are poorly understood, and designers have generally preferred to develop active devices to prevent the unknown from occurring.

Several core catcher proposals are presently in existence, including three U.S. patents; these will be briefly described in Section 1.4.3 and critically evaluated in Chapter 4. The concepts employed in all these existing proposals were known to the authors of Refs. E3 and M3, yet both concluded that although core catchers were technically feasible, no completely satisfactory method of containing the molten materials had yet been designed.

1.3 BRIEF DESCRIPTION OF THE HYPOTHETICAL MELTDOWN

Because the ability of any core catcher to retain the molten debris (core, structural components, and pressure vessel) depends on such factors as the temperature and quantity of molten material, the fission product decay heat generation within the debris, and physical/chemical reactions which may even assume an explosive nature, the sequence of events from primary coolant rupture to containment vessel meltthrough must be considered. Although many of the aspects of the accident may be occurring simultaneously, the major events may be considered to take place sequentially; broadly grouped in this manner, the events include (1) primary system <u>blowdown</u>, (2) core <u>heatup</u> without ECCS, (3) core <u>collapse</u> and collection in the reactor vessel, (4) reactor pressure vessel <u>meltthrough</u>, and (5) containment vessel <u>meltthrough</u>. The actual analytical investigation of the meltdown sequence of events is presented in Chapter 3; the purpose of this section is to introduce the accident sequence,

indicating uncertainties, an order-of-magnitude time progression, and the expected heat load on a potential core catcher.

1.3.1 Blowdown

Immediately following main coolant pipe rupture, the primary system coolant inventory is rapidly expelled into the containment, producing steam and hot air which increases the containment pressure. At a primary system pressure of approximately 600 psi for pressurized water reactors (L1,M1) the emergency accumulators of the ECCS should function to cover the core with water; however, as discussed in Section 1.2.3, it is at least hypothetically possible that the 600°F average fuel rods immediately steam bind the core (psat for 600°F is 1471 psi), preventing the accumulator water from ever reaching the core. Even if the reactor vessel is refilled, it will boil dry again in about 10 minutes, assuming the primary coolant pipe break to be unisolatable (F3). Complete primary system blowdown has been calculated to occur in 5-30 seconds after LOCA (M3). Uncertainties in this time-to-blowdown include size and location of the rupture, geometry of the break (a smooth fracture allows a greater mass flow rate than a rough, jagged tear), and the containment volume backpressure.

1.3.2 Core Heatup Without ECCS

Immediately following LOCA and the ensuing full reactor scram, with all neutron-absorbing control rods fully inserted, core power is about 6-7% of full power, under the conservative

assumption that the core has operated an effectively infinite time to achieve an equilibrium full power fission product inventory. Assuming no external removal of the decay heat from these fission products, the core materials (cladding, fuel, and structures) begin a rapid heatup, until eventually the core geometry is lost due to melting and collapsing of its structural members.

Several decay heat correlations, predicting the magnitude of the heat rate as a function of time after shutdown, are in existence, but the one used in this thesis will be that known as the "ANS Standard" proposed by an American Nuclear Society subcommittee and approved for use (when increased by 20%) by the AEC Regulatory Staff (A2). For order of magnitude heatup times, however, a correlation developed by Untermeyer and Weils, which closely reproduces the ANS Standard results, is used because of its relative simplicity and easily manipulated analytical form. Metal-water reactions between the zircaloy cladding and any available steam or water in the reactor vessel represents additional heat sources which must also be considered.

By applying an adiabatic heatup model to the core mass, an analytical expression predicting core temperatures versus time is determined (M3), providing a prediction of time to collapse. Uncertainties and inaccuracies involved in this phase of the accident are critical since a longer time of containment in the core implies a smaller heat source which must be assimilated later by a postulated core catcher. These un-

certainties include: (1) the prediction of the decay heat source (the core will not have operated an infinite time): (2) the prediction of the metal-water reaction, which is dependent upon the unknown amount of water left in the vessel; and, (3) use of an adiabatic model utilizing only the core mass (fuel, cladding, and fuel rod structures) -- conservative since some heat will be removed by radiation and conduction to the reactor vessel walls.

1.3.3 Core Collapse/Collection in the Reactor Vessel

As the core temperature increases, collapse may be predicted to occur due to melting of the zircaloy clad, melting of the uranium fuel, melting of fuel rod structural supports, or embrittlement, at a lower temperature, of any of these materials. In any event, a temperature is eventually reached which results in the core, either in large volumes or piecemeal, falling to the lower grid support. Predicted times for core collapse range from 5-15 minutes for core hot spots, and 10-60 minutes for the entire core (M3). The core continues its downward path by melting through, or collapsing, the lower grid support plate in about 30 minutes (M3).

As indicated above, the actual mechanism by which the core will fail is not known, and thus represents a major uncertainty during this phase of the accident. Potential chemical or physical reactions could occur as the molten debris falls into the vessel head area; steam explosions are a major area of concern during this phase, and could result in an energy release

of sufficient magnitude to rupture the pressure vessel, scattering fuel throughout the containment vessel. Uncertainties in calculations of the total energy resulting from the steam generation process are many: (1) the amount of water remaining in the pressure vessel is unknown, but has a significant effect on the overall transient; (2) the efficiency of the transfer process is unknown; (3) possible metallurgical reactions result in a wide range of possible debris melting points, thus affecting the amount of sensible heat available for the reaction; and, (4) the pressure pulse resulting from an assumed water enthalpy change is unknown.

Results of most studies of possible steam explosions at this point in the meltdown, while recognizing many of the uncertainties listed, conclude that pressure vessel destruction by explosion cannot be ruled out as a possibility in the sequence of events following LOCA with no ECCS (F1, F3, M3). As a result of the steam explosion potential, it also seems obvious that a premium should be attached to a core catcher design which does not rely upon the molten debris impacting water for initial cooling purposes.

1.3.4 Reactor Pressure Vessel Meltthrough

After the molten debris reaches the reactor pressure vessel bottom, and assuming a vessel-damaging explosion does not occur, the accident will either be terminated, as the debris solidifies and cools, or continue, as the debris propagates through the reactor vessel bottom head. Some analyses (M3, T2) indicate

that under special circumstances the core may in fact be coolable inside the vessel; however, a more likely occurrence is for the debris, probably at an average temperature of about 5000°F (the UO_2 melting point), to continue its downward path by melting through the reactor vessel.

The time to meltthrough depends to a large extent upon the type of heat transfer mechanism governing the melting process. Early investigations assumed a purely conductive model (E3, T2), while Ref. M3 advocates a convective process, resulting in a much larger heat transfer rate. Depending upon the heat transfer model used and upon the assumed value of the heat transfer coefficient, times to meltthrough range from 0.1 hours to 5 hours. Potentially, 250 tons of debris, including 100 tons of fuel and 150 tons of structure and cladding, at 6000°F (boiling point of UO_2) could exit the pressure vessel about one hour after LOCA. Several additional uncertainties become evident in considering the vessel meltthrough. The mode of heat transfer from debris to vessel is unknown, the resulting configuration of melted vessel and core debris is uncertain (stratification or mixing), the time for meltthrough can be determined analytically only after assuming temperatures and masses of molten debris, and how the melting proceeds (funneling or planar progression) is pure speculation.

1.3.5 Containment Vessel Meltthrough

Following reactor pressure vessel meltthrough, the molten debris will fall through an essentially voided keyway for the

bottom-mounted in-core flux mapping system (discussed in Chapter 2) until it encounters a three foot thickness of concrete, part of the Containment Base Plate with the designed purpose of acting as a biological shield and forming a part of the containment. Except for the relatively thin, easily penetrable steel skin of the barge, this is presently the last barrier existing on the platform mounted system before the core's fission product inventory is released to the environment. Applying an adiabatic heatup model to this meltthrough phase indicates that about 0.4 hours are required to totally decompose this layer of concrete.

Concrete disintegration begins around 900°F, with rapid thermal decomposition of $CaCO_3$ beginning around 1472°F (R1). The molten debris will be much hotter than these threshhold temperatures, so "melting" of the concrete begins immediately upon contact with the melt. The decomposition of $CaCO_3$ results in the production of CO_2 which raises the concern that one could overpressurize the containment vessel; however, if 50% of the base mat of $CaCO_3$ is decomposed, a volume of CO_2 equivalent to only 12% of the total containment volume results. Damage to the containment integrity is more important than the problem of CO_2 production (M3). The major uncertainty associated with this phase of the meltdown sequence is the manner in which the concrete barrier is breached. Containing about 20% by volume of water, the concrete will spall and crack as the water flashes to steam. Depending upon the physical mechanism, an open path from containment volume to the area under the containment could

occur much more rapidly than the adiabatic model would indicate. Conservatively, then, no credit will be taken for the time required to penetrate this last barrier in considering a core catcher design to be located beneath the containment shield building. Additionally, for terrestrial plants, no space presently exists under the containment as in the offshore concept; thus, previous core catcher concepts have been developed for emplacement inside the containment and could not take advantage of the additional delay experienced by the debris in melting through the containment concrete.

1.3.6 Summary

The sequence of events involved in a complete core melt-through has been discussed, indicating order of magnitude times required in the progression. In addition to the many uncertainties associated with the accident which have been discussed, an underlying assumption in this thesis is that the containment can survive the initial blowdown energy; for the offshore concept under consideration, this is valid due to the presence of a passive ice condenser system (see Chapter 2) to absorb the stored coolant energy.

As a conservative design base, it will be assumed that about 250 tons of molten debris at $6000^{\circ}F$ exit the pressure vessel and contact the potential core catcher one hour after LOCA. At this time the decay heat from a 3400 Mwt core is about 1.8×10^{8} BTU/hr, as given by the ANS Standard plus 20% as required by the AEC Regulatory Staff.

An extremely important conclusion of this preliminary survey is that the chain of events from LOCA to pressure vessel meltthrough occurs over a sufficient time span to permit operator action if required to prime, or otherwise deploy, an active core catcher design.

1.4 REVIEW OF PREVIOUS WORK

1.4.1 Foreword

To provide a proper data base for the discussion and analyses which follow, a survey of previous investigations of pertinent aspects of the core meltdown accident is in order. The following section focuses upon the work considered to be particularly applicable to the accident in an ocean environment and to core catcher design.

1.4.2 Background

Particularly concise descriptions of the potential core meltdown accident and of the subsequent active and passive means of mitigating its effects are found in TID-24226 (E3) and BMI-1910 (M3). Ref. E3 reports the work of an advisory task force appointed in 1966 to investigate the status of emergency cooling, while Ref. M3 is the last in a series of reports from Battelle-Columbus Laboratories evaluating the applicability of existing data to the description of reactor accidents.

Present calculational procedures used for predicting the thermal and hydraulic response of water-cooled reactors to LOCA and a summary of the design configurations of PWR's and BWR's are contained in Ref. Y1. This description differs from that

in Refs E3 and M3 in that the existing safeguards systems are shown to function properly to prevent gross fuel melting. Evaluations of the present-day Emergency Core Cooling System's (ECCS) capability to prevent fuel melting are summarized in the published "Testimony of the AEC Regulatory Staff" on an acceptance criteria for ECCS (A2).

Although most of the industry's effort has been devoted to means for preventing core meltdown, in particular through the design of a more efficient ECCS as discussed in A2, several core catcher concepts, including three which have been granted U.S. Patents, have been proposed to stop the molten material after meltdown has occurred and to prevent its breaching the reactor containment.

1.4.3 Bibliography

Appendix C provides a partial listing of articles and reports germane to the aspects of core meltdown for light water reactors which are the subject of the present research. Although fast reactor core catcher design is not specifically under consideration in this thesis, Appendix C lists those works applicable to the accident in Liquid Metal Fast Breeder Reactors, and which contain information possibly transferable to the present problem. A more complete listing of appropriate informational sources in the form of a computer retrieved listing of abstracts may be obtained upon request from the Nuclear Safety Information Center at the Oak Ridge National Laboratory.

1.4.4 Classification of Core Catcher Concepts

All core catchers have as their primary function the retention of the molten reactor debris, including fuel and structures, following core meltdown, in order to prevent breaching the containment boundary. However, here the similarity ends, there being many diverse methods envisioned to accomplish the same end result.

From the point of view of impact on existing plant design and operation, the most significant and obvious difference in the various proposals is in their degree of self-sufficiency. A completely passive core catcher is one which contains no moving parts, requires no external source of power, and depends upon no operator action or control signal for its proper operation. Some designs are completely passive in their concept while others depend upon some (or all) of the exceptions listed, and thus are active in concept. If existing plant piping, pumps, and prime movers can be utilized in an active catcher design, at least a portion of the premium attached to passive systems can be overcome.

Another significant difference in the method of operation of the various proposals is the manner in which the melting process is terminated, and the resulting final state of the debris. The objective of some proposals is to freeze the debris at an early stage by sufficient heat transfer to lower its temperature below the melting point, leaving the mass in a solid state; others would allow the debris to remain molten for a consider-

able period, terminating its progress through the containment by the melting of a sacrificial material, thus absorbing the decay heat in the heat of fusion of the sacrificial material, and simultaneously diluting the volumetric heat generation rate of the debris. Since the system remains in the molten state, water cooling to further remove the decay heat would remain problematical due to the potential steam explosion consideration as discussed in Section 1.3.4.

Further differences arise in the mode of heat transfer relied upon to transport energy from the debris to the catcher heat sink. As discussed above, some proposals rely strictly upon the heat absorption manifested in a change of state, the <u>heat of fusion or vaporization</u> of a "coolant". Other designs require the fission product decay heat to be transferred to an external sink by pure <u>conduction</u>, <u>convection</u>, or <u>radiation</u>, or some <u>combination</u> of the three.

Finally, it is the intent of some core catcher designs to <u>disperse</u>, or spread, the molten debris into thin layers to increase heat transfer areas, while other catchers would <u>collect</u> the debris for systematic cooling.

The core catcher proposals discussed in the following section all utilize various combinations of these principles in their final designs. Pragmatically, one additional classification is that of cost effectiveness--<u>cheap</u> versus <u>expensive</u>. Since core catchers are not presently required by the AEC, there is understandable reluctance to install an extremely expensive device which many feel will never be called upon to perform

its designed function but would be deployed merely to allay public fears of a large fission product release. Reactor designers may also be reluctant to consider core catchers in view of the fact that a defective in-vessel design is known to have actually caused a partial meltdown in the Enrico Fermi fast reactor. Additionally, offshore barge-mounted plant designers must consider the effect of the additional weight and moment contribution of the core catcher if installed. Excessive cost and/or weight/volume of a proposal could be considered to constitute a "critical flaw" for that design.

1.4.5 Description of Core Catcher Proposals

A number of conceptual designs of core catchers have been developed to the point where they can be subjected to profitable scrutiny. A brief description of these proposals follows.

Indian Point 2 Concept

The Preliminary Safety Analysis Report (PSAR) for Consolidated Edison's Indian Point 2 Reactor contained a description of a magnesium oxide-lined steel crucible which would be installed to provide an additional barrier to back up the Emergency Core Cooling System in the event of a loss of coolant accident; however, in the seventh supplement to that report the company stated that the addition of emergency accumulators to provide core reflood water relegated the crucible to a role which would never be needed, even under the worst hypothesized accident--the crucible was therefore not installed. As envisioned, the refractory-lined stainless steel crucible would

have been located at the bottom of the containment vessel dry-well which would be filled with borated water to a 30 foot height, covering the core catcher. The bottom of the crucible would then be cooled by nucleate boiling of the water, although most of the decay heat would be removed by boiling at the top surface.

BMI-1910 Spreader Concept (M3)

As an alternative to attempting to hold the core in a crucible in a molten state, BMI-1910 suggests the possibility of spreading the debris into a large diameter, thin, solid layer which could then be more easily cooled than a compact mass held in a rigidly confined crucible. This concept has been considered in detail for fast reactors where the possibility of the core recombining into a critical configuration exists: a development which is not credible for LWR's. After spreading sufficiently thin such that the debris will solidify and the layer center will remain below its melting point, water cooling is again required. The spreading process is not assured without mechanical devices to promote the spreading. The authors of BMI-1910 recommend three methods to promote the spreading process; (1) use of a convex surface under the vessel, (2) use of a "honeycomb" structure, or, (3) by melting materials in the catcher which would form solutions with, and liquefy the UO_2 at relatively low temperatures.

Doan-Crowley Lead Slurry Concept (D2)

This concept circumvents the possibility of metal-water reactions by substituting lead for water in a steel-lined

crucible in the dry well beneath the pressure vessel. The lead dilutes the volumetric heat source, increases the heat transfer area by spreading, and could float the UO_2 to prevent direct contact with the crucible. Heat transfer from the UO_2 to the lead would be primarily by convection and conduction, while the lead would transfer heat to the ambient primarily by radiation and evaporation. A variation on this process suggested by BMI-1910 would cover the lead slab with a pool of water to provide the initial core quenching.

Zivi Sacrificial UO_2 Concept (Z2)

This concept closely parallels the Doan-Crowley lead slurry concept, substituting depleted or unenriched UO_2 for lead. The second U.S. patent for a core catcher proposal was granted for this concept. The preferred method of employing this concept, as described in Ref. Z2, is to encase a dish-shaped layer of unenriched uranium dioxide in a steel liner and position it under the reactor vessel. Alternative schemes would place the device inside the reactor vessel or design it as a part of the vessel bottom. The UO_2 barrier is slowly melted by the advancing debris containing the decaying fission product heat source, and the heat is thus absorbed in the heat of fusion of the unenriched UO_2. The upper surface of the pool would be cooled by vaporizing the steel liner.

West-Fletcher Water-Cooled Crucible (W4)

Chronologically, the first U.S. patent was issued for this design concept. Here, a catch basin beneath the reactor vessel is formed by welding a bank of horizontal tubes together to

form a liquid-tight container. An upstanding rim is provided to restrain outward flow of the molten debris. The tubes are connected via an inlet header to an elevated supply of water on one end with outlet header piping of less flow resistance opening to the atmosphere somewhere above the inlet supply. Flow of cooling water through the pipe is then expected to occur automatically upon heating the basin. To ensure proper flow direction at initiation, a check valve and/or a cold water trap is installed in the inlet header. The basin area is sized to limit the debris thickness such that the top surface will remain frozen as the decay heat is removed by the cooling water. A variation of this scheme could be obtained, as suggested in the patent, by providing a layer of a second material such as lead on top of the tubes to initially quench the molten debris.

Jansen Combination Catcher Concept (J3)

A third U.S. patent has recently (1972) been granted to this concept, which employs a combination of the proposals described above. This design utilizes a ceramic oxide eutectic, such as basalt, with a relatively low melting point to dilute the reactor fuel and lower its melting temperature. A second barrier is provided by a layer of unenriched UO_2 to function as described in the Zivi Sacrificial Concept. A layer of fire brick or other refractory material then separates the steel containment vessel from the hot debris. Finally, water cooling is employed by tubes embedded in the containment vessel concrete. Although this concept was originally intended for fast reactors, it is adaptable to thermal reactors as well.

Tong In-Vessel Concept (T2)

An analysis of the asymptotic, or steady state, meltdown core configuration in Ref. T2 has indicated that a complete core meltdown can be retained in the reactor pressure vessel if the containment volume is flooded above the vessel. A cycle involving the vaporization and condensation of steel is assumed to cool the debris, removing the fission product decay heat and preventing meltthrough.

1.5 OUTLINE OF REPORT

A description of the presently envisioned offshore plant is presented in Chapter 2, indicating where consideration has been given to the maximum credible accident and discussing the volume, displacement, and moment considerations attendant to the installation of another safeguards system, the core catcher. Following this is an analytical discussion of the meltdown accident in Chapter 3. The existing core catcher proposals will be reviewed in Chapter 4, considering their suitability for use on barge-mounted nuclear plants, with the overall objective being to prevent breach of the barge confines and the resulting radioactive release to the surrounding waters. Finally, a new concept for stopping the molten mass of fuel and structural material will be described along with analytical and experimental results which verify its conceptual feasibility, in Chapter 5.

Chapter 2

CURRENT OFFSHORE CONCEPTUAL DESIGN

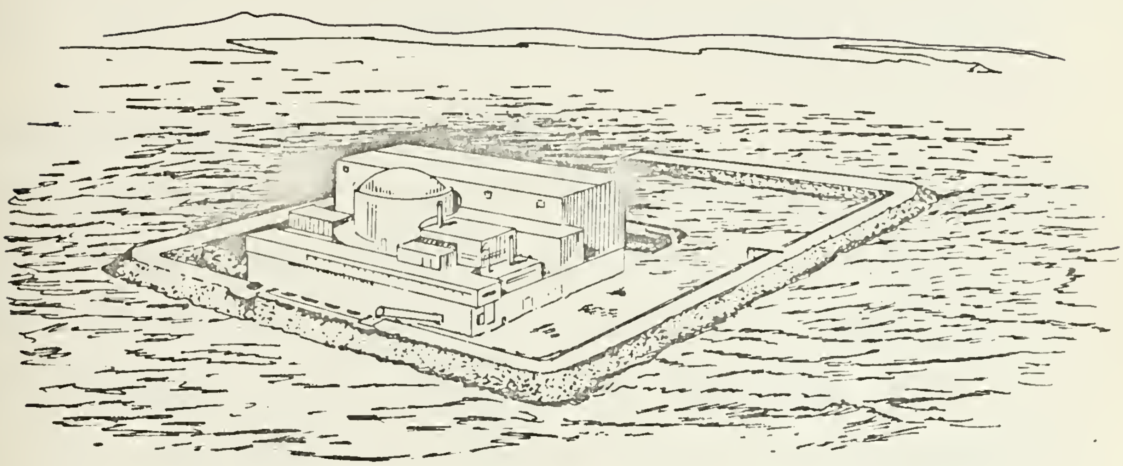
2.1 FOREWORD

In order to both devise and evaluate core catcher concepts it is essential that the envelope of design parameter constraints be established at the outset. For that reason this chapter will be devoted to the description of the one offshore design which has progressed far enough to permit specific analysis. Attention will be focused on aspects of potential plant system-core catcher interaction. The envisioned floating plant is a conventional Westinghouse pressurized water reactor and turbine-generator system utilizing that company's nuclear steam supply system, turbine generator, auxiliary systems, and safeguards systems as described in Refs. L1 and M1.

2.2 INTRODUCTION

The present design for the offshore nuclear plant envisioned by Offshore Power Systems is a joint effort of Tenneco Corporation and the Westinghouse Electric Corporation. The floating plant will be a self-contained system including heat source, steam generating and electrical producing equipment, and all required auxiliary and emergency systems, mounted on a floating platform and employing accepted and previously licensed system concepts. Floating in an average water depth of 50 feet while protected by a massive breakwater (Fig. 2.1), the plant will transmit its product power to the shore through transmission cables buried underground. The electrical con-

FIG. 2.1 THE FLOATING OFFSHORE PLANT



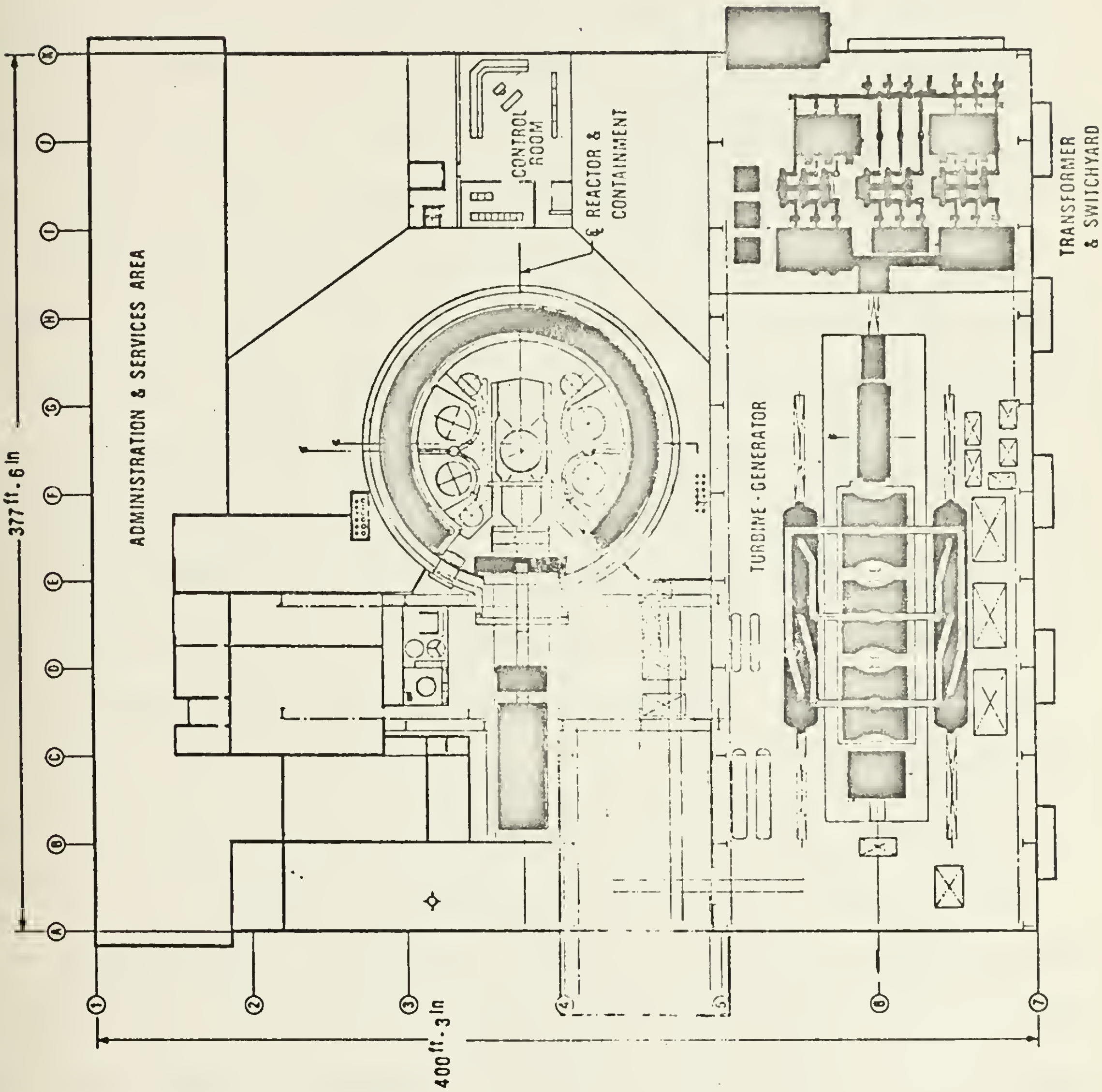
*THE CONCEPT FOR PSEG OF NEW JERSEY INCLUDES TWO COMPLETE UNITS FLOATING BEHIND A SINGLE BREAKWATER

nections, mooring system, and cooling water discharge structures are designed to accomodate horizontal and vertical barge movement as the platform follows tidal motion through the basin's common connection with the surrounding ocean. The nuclear fuel is not loaded until the plant is on station, moored, and protected by the breakwater. Because the barge is designed to remain afloat within its protective breakwater for its 40 year lifetime, an elaborate cathodic protection system will be employed. Refs. O1 and M1 provide a more detailed overview.

2.3 PLATFORM STRUCTURE AND LAYOUT

The barge which serves as the foundation for the platform is about 400 feet long, 378 feet wide, and 40 feet deep, with a Design Load Waterline of about 32 feet, and a resulting displacement of approximately 268,000 long tons (Fig. 2.2). The large, square waterplane area provides the barge with extreme stability, derived from a transverse metacentric height (GM) of 340 feet, which compares to only 20 feet for most large tankers. The moment to heel one degree is about 952,000 foot-tons, while the moment-to-trim-one-inch (MTI) is about 13,440 foot-tons; for added-weight considerations, the tons-per-inch-immersion (TPI) is about 390 tons. These hydrostatically derived values are necessary for an analysis of a potential core catcher's impact on the barge trim and draft. To meet U.S. Coast Guard requirements for a two-compartment standard of subdivision and to provide longitudinal and transverse strength for the platform and machinery weight, the barge is divided by

FIG. 2.2 THE BARGE PLANT ARRANGEMENT



40 foot deep bulkheads, spaced 37 3/4 feet apart in the transverse direction and 47 to 82 feet apart in the longitudinal direction, providing a grillage arrangement of shear webs separating the bottom shell and the strength deck at the 40 foot level as shown in Figs. 2.3 and 2.4. Additional strength is provided by panel stiffeners used on the top deck and bottom shell in conjunction with deep girders spaced 10 to 12 feet between bulkheads. The compartment located directly under the reactor vessel (area bounded by transverse bulkheads F and G and longitudinal bulkheads 3 and 4 in Fig. 2.4) is 47 feet long and 37 3/4 feet wide with the core approximately centered over the area. The watertight boundaries enclosing this compartment (area bounded by transverse bulkheads F and H and longitudinal bulkheads 3 and 4 in Fig. 2.4) encompass an area 47 feet long and 75 1/2 feet wide. The compartment under the reactor vessel (bounded by bulkheads F-G and 3-4) is further divided by deep transverse girders extending from the bottom shell to the 7 1/2 foot overhead. These non-watertight girders divide the 47 foot spacing between bulkheads 3 and 4 into 4 spaces of 11' 9" width. Perpendicular to these girders are shallow longitudinal stiffners, about 20" high and spaced about 3.15 feet apart, dividing the 37.75 foot spacing between bulkheads F and G into 12 spaces. Both the watertight and the non-watertight compartments are bounded overhead at the 7 1/2 foot level and below by the bottom shell; this results in a total watertight volume of 26,613 cubic feet, over twice the volume

FIG. 2.3 CUTAWAY VIEW OF PLATFORM COMPARTMENTATION
BELOW THE 40' LEVEL

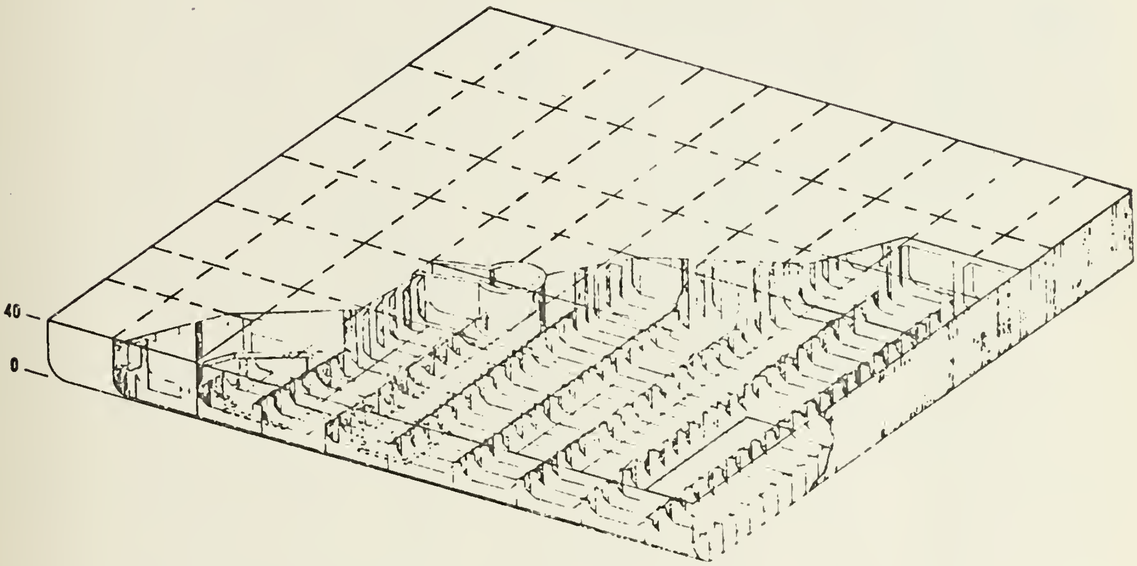
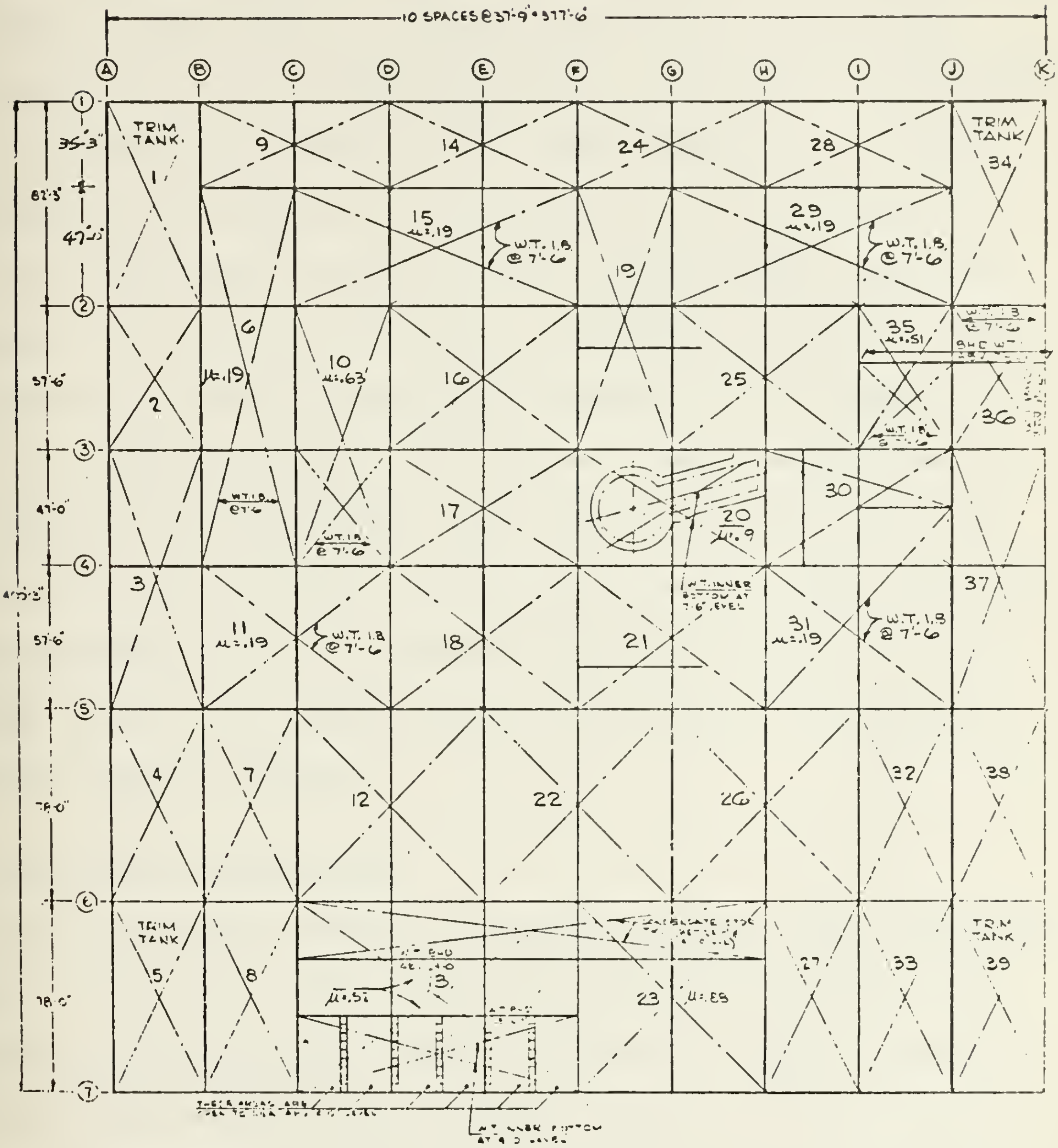


FIG. 2.4 TOP PLANAR VIEW OF COMPARTMENTATION



NOTE-BROKEN LINE "X" INDICATES BOUNDS OF WATERTIGHT COMPARTMENT

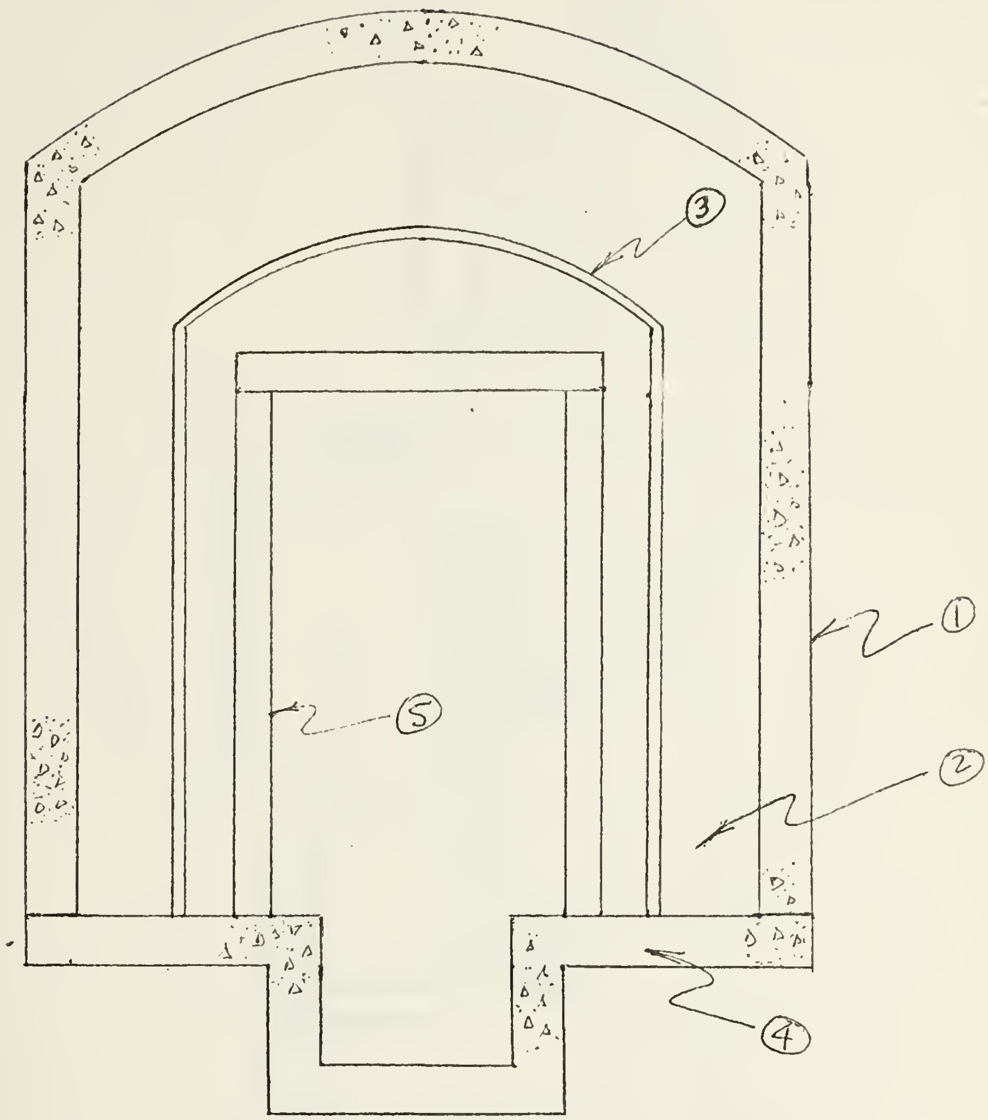
of the entire primary system coolant. Any potential core catcher concept readily adaptable to the present configuration should be contained in this compartment space. A maximum platform angle of less than 1° in pitch and 0.7° in roll has been assumed in the preliminary design, and these values should not be exceeded as a result of the added weight or moment of the proposed core catcher if backfitting is to be feasible.

The sixty spaces formed by the 7 longitudinal bulkheads (including sides) and 11 transverse bulkheads (including sides) are connected to form 30 watertight compartments, sized to ensure: (1) flooding of any two adjacent compartments does not sink any part of the 40 foot deck below the water level; (2) flooding any two adjacent compartments does not cause any part of the platform bottom to contact the ocean bottom, and; (3) flooding of any one compartment does not change the angle of trim more than one degree.

2.4 CONTAINMENT STRUCTURES

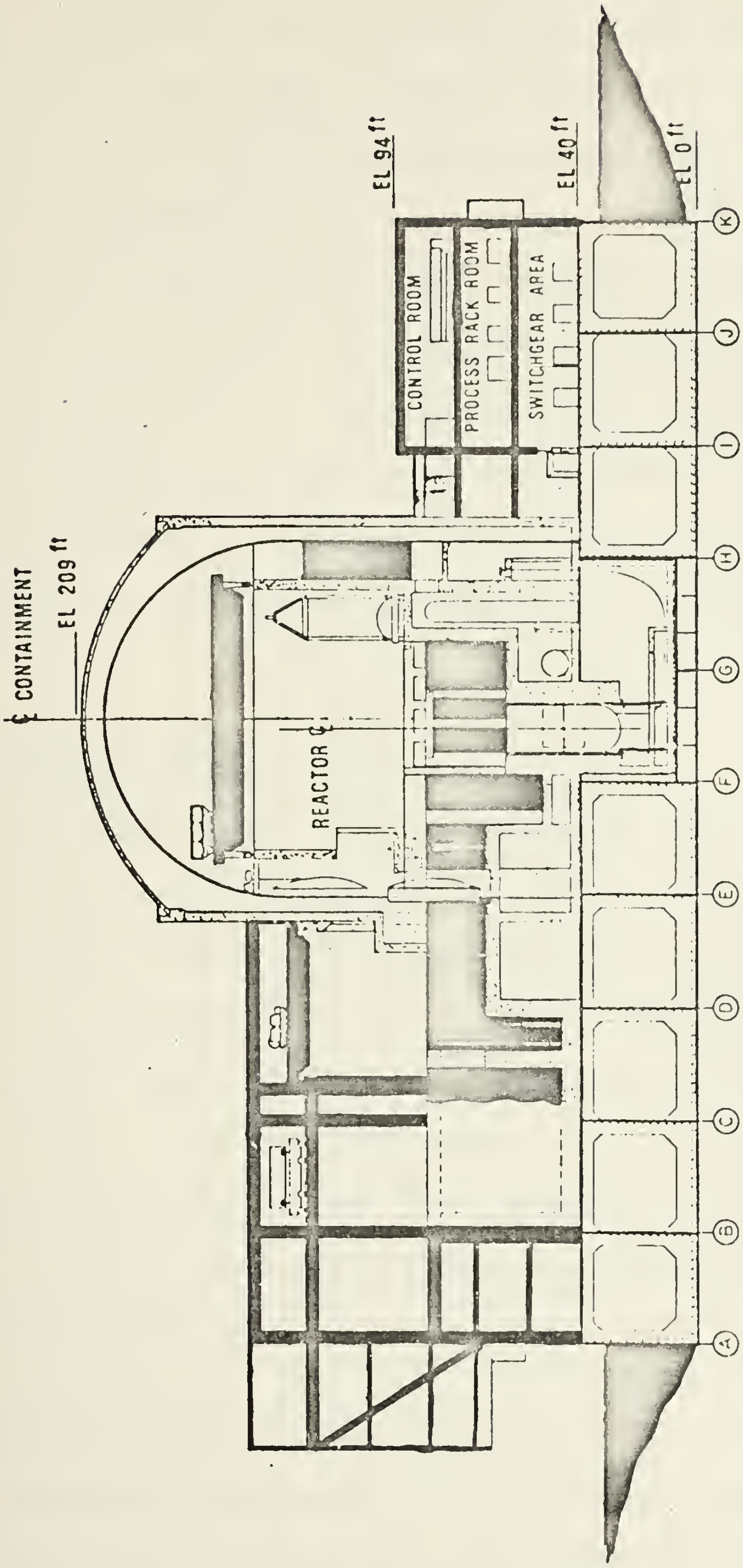
The containment assembly consists of four major structures as shown in Fig. 2.5. The assembly includes a concrete containment <u>shield building</u>, a steel containment <u>shell</u> separated from the shield building by a 5 foot annulus, a concrete containment <u>base plate</u>, and the containment <u>interior structure</u>. A scale drawing provided by the Offshore Power System in way of the containment vessel is shown in Fig. 2.6.

FIG. 2.5 THE CONTAINMENT ASSEMBLY



① Concrete Containment Shield Building
② Five Foot Annulus
③ Steel Containment Shell
④ Concrete Containment Base Plate
⑤ Containment Interior Structure

FIG. 2.6 SECTIONAL VIEW THROUGH CONTAINMENT



2.4.1 Containment Shield Building

As with many Westinghouse terrestrial-based plants, the right circular cylinder containment shield building is a reinforced concrete structure. On the 40 foot level of the platform the shield building is attached through steel connectors encased in the cylindrical concrete wall. Overall dimensions of the structure include a cylinder diameter of 130 feet, a wall thickness of 2 feet, and a crown height of 169 feet (above the 40 foot elevation). The 5 foot annular space between the concrete shield building and the steel containment shell is for access and maintenance. Constructed of 4000 psi concrete, the shield building is reinforced throughout by structural steel bars.

2.4.2 Containment Shell

The containment shell is a freestanding steel structure welded at its base to the platform at the 40 foot level. The shell is 102 feet high with an inside diameter of 120 feet. Housing the ice condenser and the pressure suppression spray system, the containment shell is designed to withstand the pressure resulting from the loss of coolant accident resulting from a double-ended pipe break. The cylinder, its hemispherical upper shell top, and the radial stiffening rings are constructed of SA 516 Grade 60 steel.

2.4.3 Containment Base Plate

The Containment Base plate consists of the upper deck of the platform at the 40 foot level inside the shield building,

the bulkhead around the reactor sump, and the innerbottom of the reactor sump at the 7 1/2 foot level (see again Fig. 2.6). In the event of the meltdown accident being postulated in this thesis (Section 1.3), the base plate forms the final barrier through which the molten fuel must penetrate prior to contacting the proposed core catcher, if the device is located in the compartment directly under the reactor vessel as described in Section 2.3.

The core catcher proposals discussed in Chapter 1 have been designed to be located inside this containment base plate; however, the space under the reactor vessel in a Westinghouse design is used to house the in-core instrumentation and is not available for the core catcher as shown in Fig. 2.7 (M1,02).

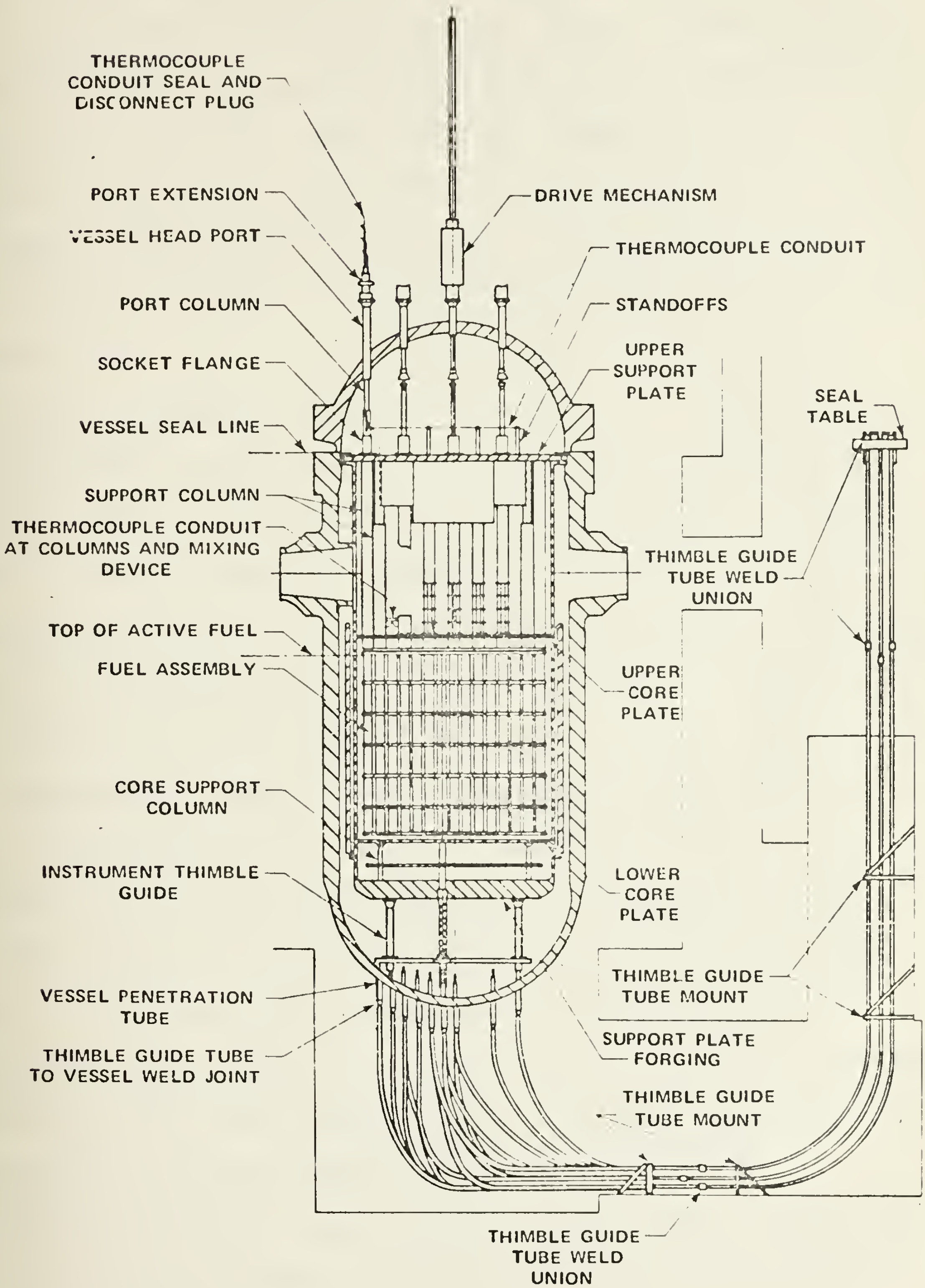
2.4.4 Containment Interior Structure

The containment interior structure provides the pressure boundary for the ice condenser, provides support for major equipment, and serves as a biological shield. It consists mainly of concrete structures including a circular crane wall, an operating deck, a refueling canal with a stainless steel liner, and the primary biological shield enclosing the reactor vessel.

2.5 ENGINEERED SAFETY SYSTEMS

The loss of coolant accident due to a double-ended main coolant pipe break, assumed by this thesis to ultimately lead to a core meltdown and pressure vessel meltthrough, is catagorized by the AEC as a "Condition IV" occurence (one which is not expected to take place but is postulated because its con-

FIG. 2.7 REACTOR VESSEL CONFIGURATION ILLUSTRATING IN-CORE INSTRUMENTATION CONDUITS



sequence would include the potential for the release of significant quantities of radioactive material) (O1). Listed in Table 2.1 are the so-called Engineered Safety Systems designed to function only in emergency situations, and in particular, in the event of a LOCA. This table also shows potential interactions of each safety system with a hypothetical core catcher. THIS THESIS TACITLY ASSUMES THAT ONE OF THE SYSTEMS (ECCS) FAILS IN ITS PRIMARY FUNCTION TO REMOVE THE DECAY HEAT FROM THE CORE FOLLOWING LOCA.

Another engineered system, the breakwater, while not listed as an Engineered Safety System, provides protection from ship collisions which could result in sufficient damage to produce a chain of events leading to a meltdown. Although interviews with onboard witnesses reveal that there is no danger from <u>shock</u> damage in a collision (for all ocean collisions of record there is no evidence of shock-induced failure of piping or machinery) (W3), <u>penetration</u> damage could conceivably rupture the main coolant piping <u>and</u> render the ECCS inoperable. The breakwater (estimated for the conceptual design of the offshore plant to cost $200 million) is therefore designed to prevent any ship capable of traversing the ocean area in which the plant is located from penetrating its barrier and reaching the barge (D4). It may also serve some function in controlling dispersion of radionuclides released within its confines.

The following is a brief description of each of these systems, indicating the design function assuming proper operation.

Table 2.1

ENGINEERED SAFETY SYSTEMS AND POTENTIAL CORE CATCHER INTERACTIONS

System	Possible Interactions with Core Catcher
Emergency Core Cooling	1. Proper operation precludes necessity for catcher.
Containment Spray	1. Can provide 350,000 gallons of water for catcher cooling. 2. System piping, pumps, and heat exchangers available for catcher cooling. 3. Maintains containment pressure ≤ 10 psi by condensing steam evolved from catcher and from system blowdown.
Ice Condenser	1. Can provide 314,000 gallons of water for catcher cooling. 2. Maintains containment pressure ≤ 10 psi by condensing steam evolved from the catcher and from system blowdown.
Emergency Power	1. Provides either off-barge or diesel power to drive cooling pumps or to provide control signals.
Essential Service Water	1. Acts as ultimate heat sink for decay heat from the catcher by removing heat from the Containment Spray heat exchangers.
Annulus Filtration	1. Vents through filters the airborne radionuclides entering the 5 foot annulus between the shield building and containment shell, minimizing atmospheric radioactive release.
Containment Isolation	1. Forms part of pressure boundary, along with containment to prevent fission product release via containment piping penetration.
Hydrogen Recombination	1. Maintains hydrogen concentration < 2% in containment. (Flammability limit 4% and explosive limit 18%).

Table 2.1 (con't)

Containment Venting	1.	Acts as backup to Hydrogen Recombination System to allow controlled venting of containment volume through charcoal filters.
Drain System	1.	Remove water from catcher compartment to prevent molten debris impacting water.

2.5.1 Emergency Core Cooling System

Following LOCA, the ECCS provides a means to reflood an intact reactor vessel and remove the fission product decay heat to assure core integrity, to maintain the essential core geometry for heat transfer, and to limit the metal-water reaction rate to that which can be accomodated by the Hydrogen Recombination System. In particular, this system is required by the Interim Acceptance Criteria for Emergency Core Cooling Systems (A2) to limit fuel clad temperatures to less than 2300° F and to prevent greater than 1% of the zircaloy cladding from undergoing metal-water reactions with the subsequent production of excessive hydrogen. A properly functioning ECCS will therefore render a core catcher superfluous.

2.5.2 Containment Spray System

The Containment Spray System, together with the Ice Condenser Systems, is designed to prevent the maximum pressure in the containment shell from exceeding 10 psig as the result of the complete blowdown of coolant through any pipe rupture up to, and including, the double-ended break. Specifically, the two systems are designed to remove all stored energy from the lost coolant, the heat of reaction resulting from a 33% zircaloy-water reaction, with the produced hydrogen being burned as it is released, 5×10^7 BTU's (about 16% of the coolant stored energy) of "undefined" energy, and a continuous release, as steam, of all the core residual heat. The Containment Spray system also functions to scavenge airborne nuclides, mitigating the con-

sequences of vapor release to the atmosphere. The spray water initially is provided by the Suction Supply System from two tanks with a usable capacity of 350,000 gallons of borated water, then suction is shifted to the containment sumps to continue recirculating water from the sumps through the four contain-spray heat exchangers and back to the containment, followed by a 75 foot vertical spray fall through the contained space and finally, drainage back into the sumps. Note that if required by an actively cooled core catcher this 350,000 gallons (4.7×10^4 cubic feet) could easily be made available to the compartment under the reactor vessel by the installation of piping into that area. Thus, the system's heat exchangers would be used to remove decay heat from a water cooled crucible containing the molten or solidified core debris. The four shell-and-tube heat exchangers are serviced by the Essential Service Raw Water System, and have a combined capacity of 5.6×10^7 BTU/hr. The design conditions for the heat exchangers are listed in Table 2.1.

Table 2.1

DESIGN DATA FOR CONTAINMENT SPRAY HEAT EXCHANGERS

	Shell (Raw Water)	Tube (Spray Water)
flow, gpm	3000	2400
inlet temp, ^oF	85	165
outlet temp, ^oF	123.4	117.2

2.5.3 Ice Condenser System

The Ice Condenser system contains about 2 1/2 million pounds of ice, sized to provide an energy absorption capacity of twice the total coolant energy released during blowdown. Completely

enclosed in an annular compartment located around the perimeter of the upper containment volume (see Fig. 2.8), the ice bed is isolated from the containment environment by normally-shut upper and lower hinged doors which open to expose the ice almost immediately upon LOCA due to the pressure rise in the associated compartment. Note that upon melting, the ice potentially provides 4.2×10^4 cubic feet (3.14×10^5 gallons) of water to cool a core catcher requiring active cooling in conjunction with the Containment Spray System as described above.

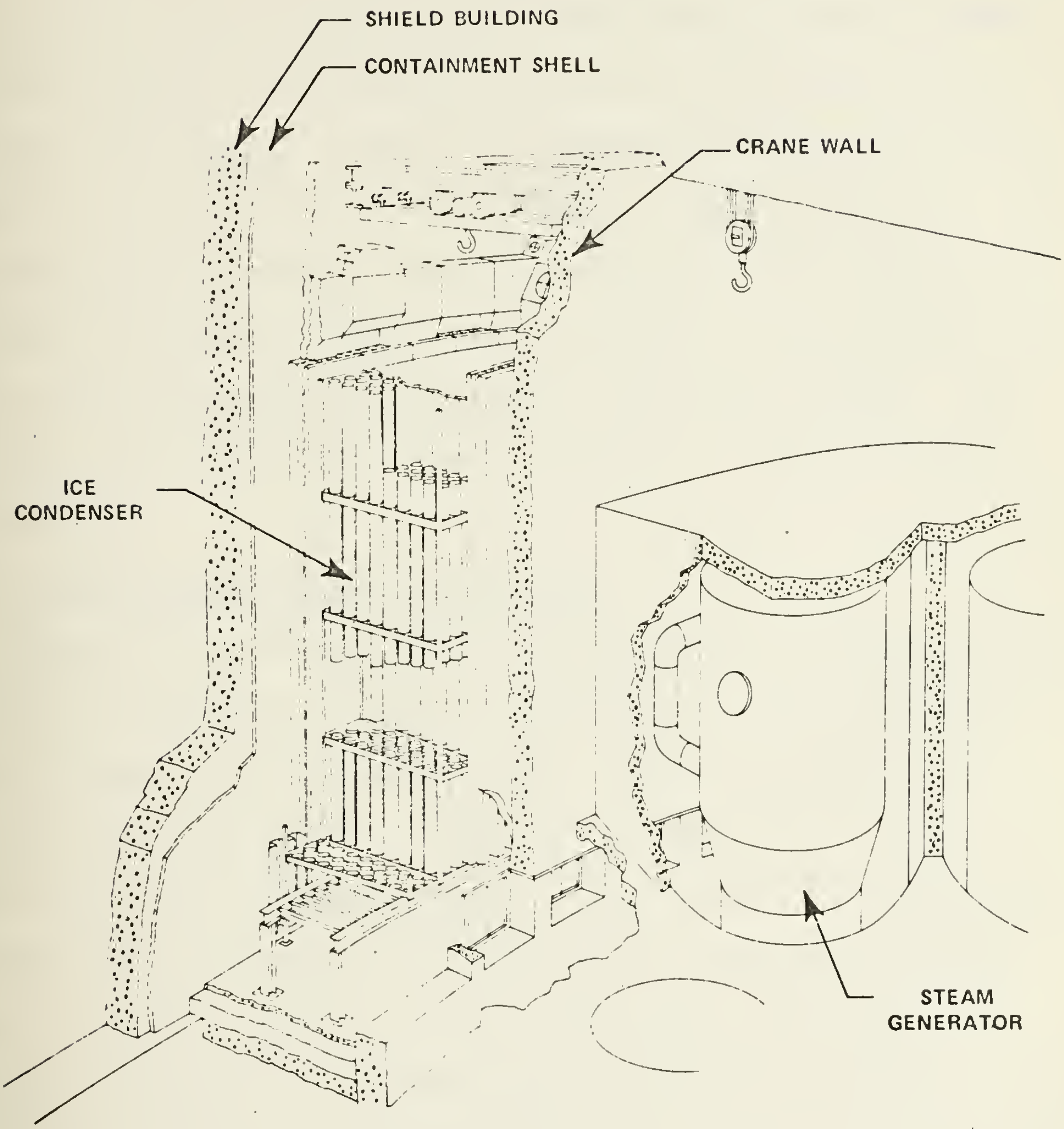
2.5.4 Emergency Power System

In the event of plant trip with the resulting loss of electric power such as would occur following LOCA, the station auxiliary transformer remains connected to one shore cable, and off-site power continues to be available without interruption. As a backup power source, sufficient self-contained diesel power is available to power all emergency equipment on the barge. Provision has therefore already been made in the barge design for core catcher designs requiring an external source of pumping power to provide cooling water; either presently installed pumps, such as the containment spray pumps, or a separate set of core catcher cooling pumps could be sustained by this emergency power.

2.5.5 Essential Service Water System

The Essential Service Water System supplies sea water cooling to the Containment Spray System heat exchangers through four completely independent trains. The four Essential Service water

FIG. 2.8 ICE CONDENSER SYSTEM



pumps are automatically placed on the emergency diesel output in the event of a total loss of off-site power. For a core catcher design utilizing water cooling, this system could act as the final heat sink for the fission product decay heat by pumping the catcher cooling water (with the Containment Spray Pumps, for example) through the tube side of the Containment Spray heat exchangers. Recall that these heat exchangers have a combined capacity of 5.6×10^7 BTU/hr, so that initially, with an assumed fission product decay heat of 1.8×10^8 BTU/hr, some heat must be removed by other means, perhaps through water evaporation and condensation on the steel Containment Shell. The decay heat value falls to 5.6×10^7 BTU/hr in about 2.3 days after shutdown.

2.4.6 Other Engineered Safety Systems

Several of the Engineered Safety Systems (Annulus Filtration, Containment Isolation, Hydrogen Recombination, and Containment Venting) impact the hypothetical core catcher by functioning as normally designed and are adequately described in Table 2.1 and Ref. 02. All of these systems are important in mitigating the effects of a reactor core meltdown and preventing a gross release of fission products to the atmosphere, but none will be utilized directly by a core catcher design in containing the molten debris meltdown products. The drain system is included in this table, although not an Engineered Safety System.

2.6 SUMMARY

The presently envisioned offshore power system has been described, indicating the systems and containment designed to

mitigate the effects of LOCA and to prevent core meltdown. The compartment space available for the location of a potential core catcher was described; and Table 2.1 reviews possible installed system interactions with the hypothetical catcher.

The next chapter will describe the postulated core meltdown accident in a more analytical fashion. Chapter 4 will then evaluate existing core catcher proposals in view of the specific offshore plant configuration and their resulting impact upon it, as inferred from the description presented in this chapter. Chapter 5 then presents a new core catcher concept, including analytical and experimental verification of its feasibility.

Chapter 3

ANALYSIS OF THE MELTDOWN ACCIDENT

3.1 FOREWORD

Information introducing the offshore concept and discussing the need for additional core-meltthrough barriers was presented in Chapter 1, along with a brief summary of previous work addressing this problem. Chapter 2 describes in some detail the presently envisioned offshore concept, indicating potential system interactions with a hypothetical core catcher device. This chapter, an analytical evaluation of the meltdown sequence, expands upon the qualitative information presented in Section 1.3, to develop the quantitative aspects required to evaluate the capability of a proposed core catcher design. Chapters 4 and 5 will then use this information to assess several core catcher proposals. Values of the physical parameters used are given in Appendix A; the nomenclature used is defined in Appendix B.

3.2 BLOWDOWN

The time required to expel the primary system water from the reactor vessel, pressurizer, and coolant piping is a direct function of the driving force, the differential pressure between the primary system and the containment volume. Initially this pressure differential will be around 2200 psi, but will decrease as the containment pressure increases and primary loop pressure decreases; note that a properly functioning Containment Spray and Ice Condenser Systems discussed in Chapter 2 will act to

suppress the containment pressure, thus enhancing the latter stages of the blowdown. A minimum blowdown time of 5-10 seconds and a maximum of 30-45 seconds has been calculated (E3).

At the end of blowdown (25 seconds) approximately 10% of the pressure vessel's initial volume of water remains in the vessel, and fuel rod temperatures for a typical 1000 MW electric plant are given by the expression,

$$T(25)^{o}F = 928(F-0.33) + 540^{o}F \qquad (3.1)$$

where F is a local power peaking factor which varies between 0.36 and 2.1; Eq. 3.1 reproduces the TACT computer code results within $\pm$ 50°F (M3).

3.3 CORE HEATUP WITHOUT ECCS

The post-shutdown fission product decay heat rate is 6-7% of the full power rating of the reactor core immediately upon shutdown, under the conservative assumption of an infinite irradiation time at full power preceeding the shutdown. The fissioning of fuel nuclei results in the production of many unstable nuclei, each of which can undergo an average of five successive stages of β^- decay, producing a new radionuclide at each step. In all, over 300 nuclides, distributed among many different chemical species are known or expected to exist after fission, making calculations involving fission product mixtures extremely difficult under the most well-defined conditions. The system of differential equations governing the formation and removal by decay and burnup of radioisotopes in nuclear fuels have

been programmed for computer calculations and the results reported by Blomeke and Todd in Ref. B3. Since the fission product concentration, and thus the decay heat generation rate, is a function of irradiation time and decay time, the authors computed the consequences of the thermal fission of U^{235} over a wide range of irradiation and decay times. A variety of useful data, such as precise fission yield, decay schemes and energies, cross sections, and fission product concentrations may be determined from this report.

Although the total beta and gamma decay power as a function of time after shutdown represents the summation of these complicated decay chains for many different nuclides, the resulting energy release can be approximated by relatively simple analytical expressions. A rather large number of correlations for the magnitude of the decay heat generation rate exist in the literature; hence, to promote conservative standardization of decay heat correlations by vendors, an American Nuclear Society subcommittee (ANS-5) recommended a standard, which was approved as the "ANS Standard" in June, 1968 (A2). This ANS Standard required that the calculations by K. Shure (S3) be used in LOCA calculations. It should be noted that the principle contribution to the decay heat comes from the decay of U^{239} and Np^{239}, both of which are included in Shure's data. The ANS Standard, increased by 20%, was accepted by the AEC Regulatory Staff for use in the "Interim Acceptance Criteria for Emergency Core Cooling Systems" (A2) as a conservative prescription for the decay heat source term in LOCA calculations, and it is now used by

all of the various vendors in their analyses. For order-of-magnitude analyses, either the Untermeyer-Weills correlation or the Way-Wigner correlation, both of which take the form of simple analytical expressions, is considered sufficiently accurate for use (C4).

The Shure correlation increased by 20% and now required by the AEC, makes use of an average value of energy release during normal power operation of 200 Mev/fission. The decay heat rate at time ts for a reator which has operated at power P for to seconds is then given by:

$$q(\text{watts}) = \frac{P(\text{watts})\ M(to,ts)}{200} \tag{3.2}$$

where M = energy decay rate, Mev/fission and

where $M(to,ts) = M(\infty,ts) - M(\infty, to + ts)$,

and $M(\infty,ts)$ and $M(\infty, to + ts)$ are

determined from Figs. 3.1 and 3.2.

Or, conservatively, for an assumed infinite irradiation,

$$q(\text{watts}) = \frac{P(\text{watts})\ M(\infty,ts)}{200}. \tag{3.3}$$

For infinite irradiation at 3400 MW thermal and at a blowdown completion time of 25 seconds, from Fig. 3.1 $M(\infty,25)$ = 8.7 Mev/fission and q(25), the decay heat rate, is 147.9 MW, or about 5.05×10^8 BTU/hr. At a shutdown time of one hour, $M(\infty,3600)$ = 2.6 Mev/fission and q(3600) = 44.2 MW = 1.5×10^8 BTU/hr. Increasing this value by 20% results in 1.8×10^8 BTU/hr at the assumed time of entry of the molten debris into a hypothetical core catcher device.

For comparative purposes, the Untermeyer-Weills correlation

FIG. 3.1 SHURE DECAY HEAT CORRELATION I

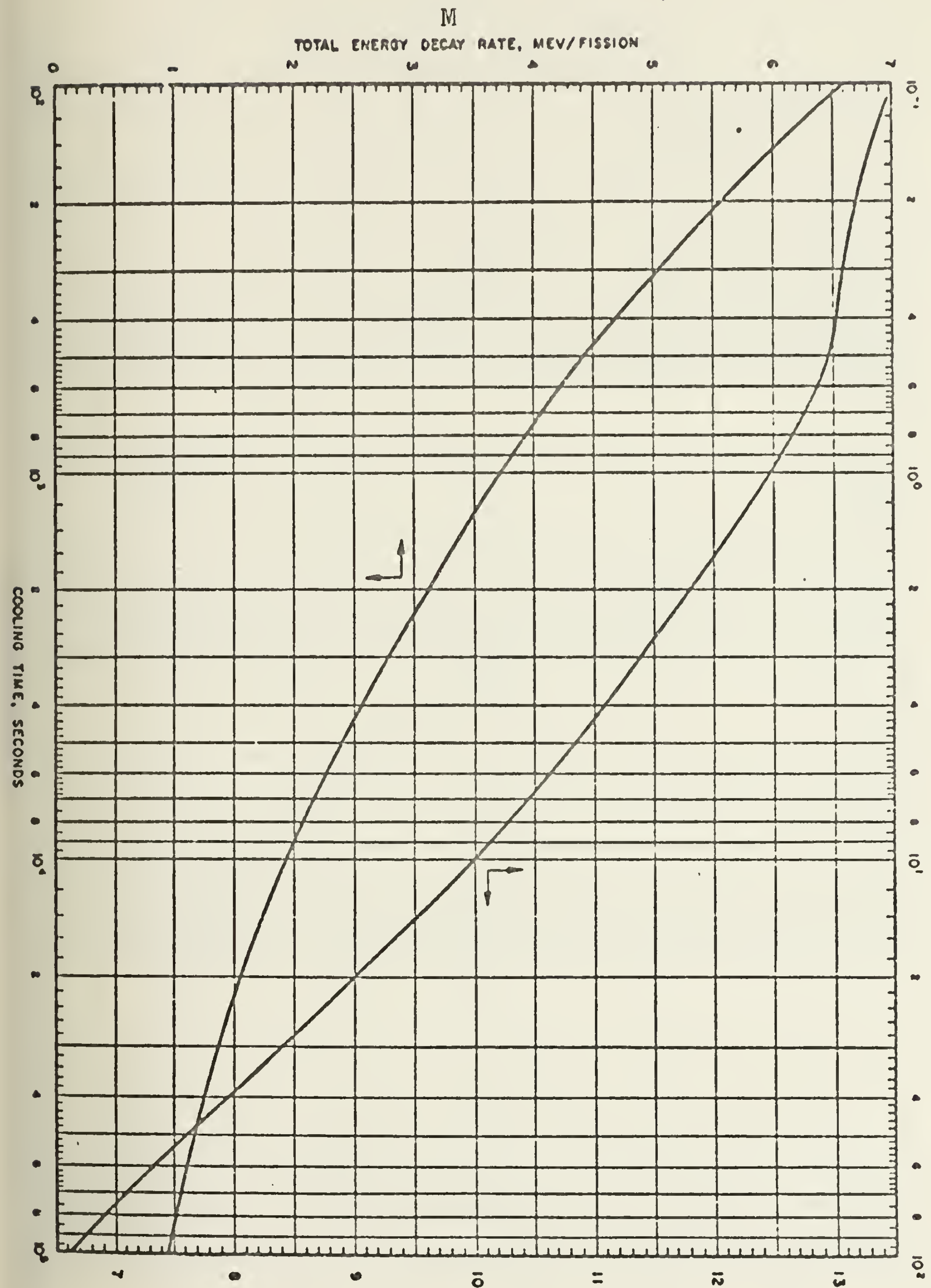
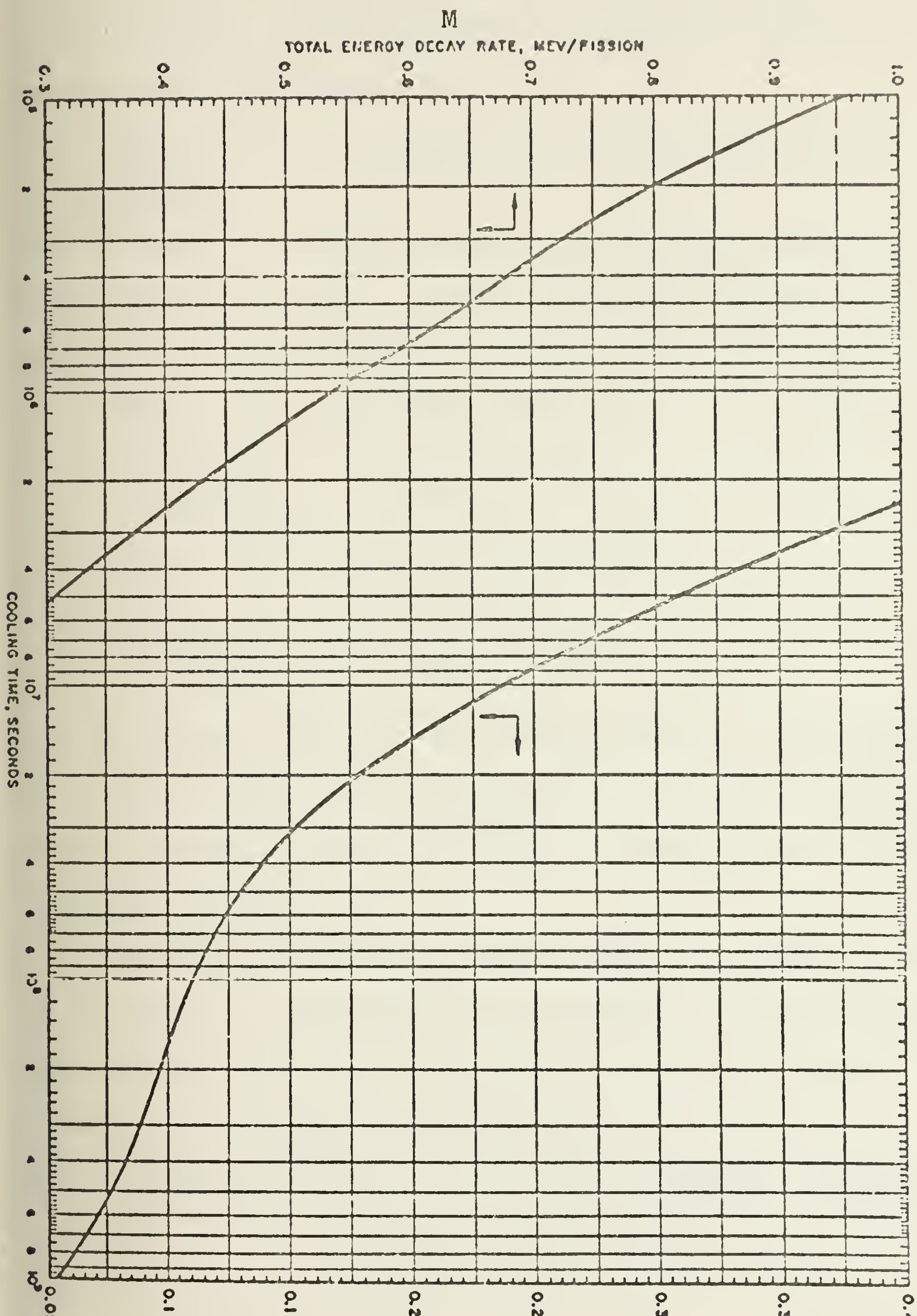


FIG. 3.2 SHURE DECAY HEAT CORRELATION II



is given by:

$$\frac{q(t_o,t_s)}{q_{operating}} = \begin{array}{l} 0.1\ (t_s+10)^{-0.2}-(t_o+t_s+10)^{-0.2}\ - \\ 0.087\ (t_s+2x10^7)^{-0.2}+(t_s+t_o+2x10^7)^{-0.2} \end{array} \quad ,(3.4)$$

or for infinite irradiation,

$$\frac{q(\infty,t_s)}{q_{operating}} = 0.1\ (t_s+10)^{-0.2} -0.087(t_s+2x10^7)^{-0.2}. \quad (3.5)$$

For the same conditions as used in the Shure calculation, the Untermeyer correlation results in values of $q(25)=5.34x10^8$ BTU/hr and $q(3600)=1.9x10^8$ BTU/hr, slightly higher than the ANS standard.

Finally, the Way-Wigner correlation is given by:

$$\frac{q(t_o,t_s)}{q_{operating}} = 0.0622(t_s^{-0.2}-(t_s+t_o)^{-0.2}). \quad (3.6)$$

For infinite operating time this reduces to

$$\frac{q(\infty,t_s)}{q_{operating}} = 0.0622(t_s)^{-0.2}. \quad (3.7)$$

For the example cases at shut down times t_s = 25 seconds and t_s = 3600 seconds, this correlation gives $q(25) = 3.8 \times 10^8$ BTU/hr and $q(3600) = 1.4 \times 10^8$ BTU/hr, slightly lower than the Shure correlation value.

Assuming an adiabatic heatup model, a total core heat capacity of 4.07×10^4 BTU/^{o}F for a typical 1000 MWe nuclear plant, and utilizing the relatively simple Way-Wigner expression, the overall core temperature change with time is given by:

$$\frac{dT}{dt} = \frac{0.0622\ t_s^{-0.2} x 3.4x10^9\ \text{watts} x 3.412\ \frac{\text{BTU/hr}}{\text{watt}} \times \frac{\text{hr}}{3.6x10^3\text{sec}}}{4.07 \times 10^4\ \text{BTU}/^o\text{F}} \quad (3.8)$$

and, $\frac{dT}{dt} = 4.92F\ t^{-0.2}\ \frac{^oF}{sec.}$ (3.8b)

For the average core temperature rise (F=1.0) due to decay heat sources,

$$\frac{dT}{dt} = 4.92\ t^{-0.2}\ \frac{^oF}{sec.} \quad (3.8c)$$

As the core temperature rises above about 2300°F, another heat source, the zircaloy-water reaction, becomes significant, the reaction rate increasing exponentially with temperature (F1). The Interim Acceptance Criteria for Emergency Core Cooling Systems requires the ECCS to limit clad temperatures to less than 2300°F in order to limit this energy release and to prevent excessive loss of cladding ductility (A2). Because the reaction depends upon temperature and upon the amount of water (steam) available for reaction, the authors of BMI-1910 (M3) have utilized a computer code developed for the Brown's Ferry Reactor to predict the magnitude of this source; the results indicate that a total of 10% of the zircaloy will react after 1000 seconds, the rate remaining fairly constant as the decrease in available steam counteracts the effect of rising temperature. Thus, assuming 44,500 lbm of zircaloy and a heat of reaction of 2800 BTU/lbm, the heat generation per second from this source is,

$$q(\text{zircaloy-water}) = 44{,}500\text{lbm} \times \frac{0.10}{10^3\text{seconds}} \times \frac{2800\text{BTU}}{\text{lbm}} \quad (3.9)$$

$$= 1.25\times10^4\ \text{BTU/seconds}$$

with a resulting core temperature rise of

$$\frac{dT}{dt} = \frac{1.25 \times 10^4 \text{ BTU/seconds}}{4.07 \times 10^4 \text{ BTU/}^{\circ}\text{F}} = 0.307 \quad {}^{\circ}\text{F/sec.} \tag{3.10}$$

The average core temperature following blowdown, at time 25 seconds and assuming no heat losses, is then

$$\begin{aligned} T(t) &= T(25) + \quad \text{Decay Heat} \quad + \quad \text{Metal-Water Heat} \\ &= \text{Eq. (3.1)} + \quad \text{Eq.(3.8b)} + \text{Eq(3.10)} \end{aligned} \tag{3.11}$$

$$T(t) = 928(F-0.33)+540+6.15F(t^{0.8}-13.1)+0.307(t-25) \tag{3.12}$$

A similar analysis in BMI-1910 (M3) resulted in the expression

$$T(t) = 928(F-0.33)+540+8.6F(t^{0.8}-13.1)+0.48(t-25) \tag{3.13}$$

which was in substantial agreement with temperatures obtained from computer codes NURLOC and CHEMLOC, leading to the conclusion that the adiabatic model is sufficiently accurate for use.

3.4 CORE COLLAPSE/COLLECTION IN THE REACTOR VESSEL

From Eq. 3.12 the time for the average core (F=1.0) temperature to reach the melting point of zircaloy (3360°F) can be determined to be around 1300 seconds (21.7 minutes) following completion of blowdown. For the core hot spots (F=2.1), and assuming that local maximum temperatures occur at the same points as local power maxima, the time to reach the zircaloy melting temperature is 310 seconds (5.2 minutes). The time for the average core to reach the UO_2 melting point (5072°F) is about 2500 seconds (41.7 minutes), while local rod hot spots will exceed this temperature in about 840 seconds (14.0 minutes).

The actual mechanism by which the core will fail is not

known. Failure could occur due to melting of the zircaloy, due to embrittlement and subsequent fracture of the zircaloy, or due to melting of the UO_2; however, fuel slumping is most likely to occur somewhere in the range of temperatures 3300°F to 5000°F. From Eq. 3.12, the core hot spots (F=2.1) reach these temperatures in about 5 minutes and 15 minutes respectively, while nearly all regions of the core will have reached these temperatures and slumped to the lower support plate forging in 10 minutes to 60 minutes (M3). The authors of BMI-1910 predict support plate failure at 1300°F in about 30 minutes (a time when about one-half of the core is molten) allowing the core to fall to the pressure vessel bottom head area.

As the molten debris falls into the reactor vessel head area, potential metal-water reactions from two sources could result in explosions of sufficient energy to rupture the vessel. The first explosive source, the generation and subsequent rapid burning of hydrogen from a reduction reaction described by

$$M + x\,H_2O \longrightarrow MO_x + x\,H_2 \ , \qquad (3.14)$$

has been determined to be an insignificant explosive source; experimental results indicate the hydrogen generation from molten UO_2 reacting with water in this fashion is only about 12 ml STP per gram of UO_2 (G2). The second explosive source is much more important, and results from rapid flashing and expansion of water to steam (with the resulting PV work); this is appropriately termed a "steam explosion". At the end of blowdown about 10% of the original pressure vessel volume of water

remains in the lower head in the form of a two-phase saturated mixture at the pressure of the containment (A2). Assuming 10 psig containment pressure (design pressure for the Offshore Power Systems containment vessel), this implies several thousand gallons of water at around 240°F are available for a steam explosion reaction.

Although no experimental data is available on the effects of the interaction between large quantities of molten UO_2 and water, many industrial and laboratory-simulated explosions involving other molten materials (aluminum, copper, silver, steel) have occurred. The energy available for the explosion is primarily the sensible heat of the molten material, and if the melt freezes, the latent heat of fusion of the material is also potentially available. Because of the rapidity of the interaction, however, experimental work with aluminum has shown that only the sensible heat of the melt is important, and the latent heat (and also the sensible heat of the solid) can be neglected in explosion considerations (W4). Experiments with molten aluminum indicate that about 10% of the total thermal energy available is converted into high pressure steam, in contrast to only 0.1% from a thin solid aluminum surface (F1). Many other experiments and much industrial operating experience support the observation that multi-thousand pound destructive forces cannot be generated from solid surfaces interacting with water; apparently the key factor is determining whether the interaction of hot material with relatively cold liquid will result in destructive pressure generation is the rate at which heat is trans-

ferred--with solids the available heat transfer area is quickly blanketed with steam and a protective oxide coating inhibits the heat transfer. Thus, with molten material, the explosion process appears to be enhanced through dispersion of the molten material and the resultant increased heat transfer surface area.

Kazimi, et al., (K1) have identified four modes of fragmentation which facilitate heat transfer to the cold liquid from the molten material, including: (1) impact fragmentation in which the metal is dispersed by an external shock source; (2) entrapment fragmentation in which the material is dispersed by the rapid vaporization of coolant trapped between the hot molten matter and a solid surface; (3) hydrodynamic fragmentation in which non-uniform drag forces around the molten surface exceed the surface tension, pulling the material apart; and, (4) free-contact fragmentation in which only a small part of the surface is contacted by the coolant while the rest of the surface is not wetted and is free to deform. Recognizing the free-contact mode of fragmentation as the mode most probable in fuel meltdown for in-vessel considerations, Kazimi has presented a criterion for fragmentation, requiring the melting temperature of the hot material to be less than the minimum temperature required to sustain stable film boiling at the surface of the molten material. Numerically,

$T_H^* > T_m$ for fragmentation, where

$$\frac{T_H^* - T'_{SAT}}{T'_{SAT} - T_{water}} = 0.42 \left[\sqrt{\frac{(\rho K C_p)_{water}}{(\rho K C_p)_{UO_2}}} \; \frac{h_{fg\,water}}{C_{p\,UO_2}(T'_{SAT} - T_{SAT})} \right]^{0.6} \qquad (3.15)$$

$$T'_{SAT} = T_{SAT_{water}} + 0.127 \frac{\rho h_{fg}}{K} \left[\frac{g(\rho_\ell - \rho_v)}{\rho_\ell + \rho_v} \right]^{2/3} \left[\frac{g_c \sigma}{g(\rho_\ell - \rho_v)} \right]^{1/2} \left[\frac{\mu}{g_c(\rho_\ell - \rho_v)} \right]^{1/3} \qquad (3.16)$$

and where all values in the expression for T'_{sat} are those of water, here assumed saturated at $240^{\circ}F$. By inserting the appropriate physical property values from Appendix A and Ref. K2,

$$T'_{sat} = 240 + 0.127\frac{(0.06)(952)}{0.39}\left[\frac{(32.2)(59.2-0.6)}{59.2+0.06}\right]^{2/3}\left[\frac{0.004}{59.2+0.06}\right]^{1/2}\left[\frac{0.7}{(32.2)(59)}\right]^{1/3}$$

$$= 240.11^{\circ}F$$

$$T_h^* = 240.11 + (0.11)(0.46)\left[\sqrt{\frac{(59.14)(0.39)1}{(600)(2.6)(0.1)}}\ \frac{952.0}{(0.1)(0.11)}\right]^{0.6}$$

$$= 264.95^{\circ}F$$

Therefore, one finds that $T_h^* < T_m(UO_2)$, and thus that this mode of fragmentation and area enhancement is not expected. Similar results are obtained for molten zircaloy and steel in water. As an aside, it should be noted that this phenomena is expected to occur in LMFBR's where the liquid coolant has a boiling point (T_{sat}) of around $1600^{\circ}F$ at atmospheric pressure, resulting in $T_h^* > T_m(UO_2)$.

Although the steam generation process and possible subsequent explosion is not expected to be enhanced by fragmentation, sufficient energy (depending upon the quantity of water remaining in the reactor vessel and the method of entry of the molten debris into the water --either "dribbling" in or in a large mass) may exist to result in a large amount of steam generation and PV-type work. It should be noted that 1 lbm of water heated from saturated liquid to saturated vapor at $240^{\circ}F$ results in an enthalpy change of 952 BTU's; Ref. M3 equates 1600 BTU's to 1 pound of TNT in destructive potential--thus 1 lbm of water totally vaporized could then yield an explosion equivalent to 0.6 pound TNT. Several uncertainties, besides the obvious concern over steam explosions, involved in this stage of the analy-

sis were pointed out in Chapter 1. The description of the meltthrough chain of events will continue, assuming no steam explosion occurs at this point.

3.5 REACTOR VESSEL MELTTHROUGH

At this point in the accident the entire 3400 MW thermal core is assumed to have collected on the reactor vessel bottom head. If the core has crumbled to the bottom in a nonmolten state due to loss of clad integrity, calculations in BMI-1861 indicate that the mass may be coolable, given a means for adequate recirculation of coolant water. For molten cores, analyses by Phillips Petroleum Company personnel and by L.S. Tong (T2) indicate meltthrough may be stopped in-vessel under certain circumstances. The Phillips results, however, assume the molten core can be cooled to 250°F by dropping it through pressure vessel water, and even given this doubtful accomplishment, the results show meltthrough will occur if greater than 25% of the core melts (M3). Analyzing the asymptotic, or steady state, situation, Tong assumes the entire reactor vessel will be submerged in water or cooled by spray and then relies on a boiling-condensing cycle of steel to cool the debris; however, a detailed analysis of Tong's work indicates that less than 1/4" of reactor vessel bottom head thickness will be under 800°F, and the vessel will lack the structural strength required to support the molten mass (F3).

From data given in Appendix A for the volume and area of

the lower head, an equivalent hemisphere of diameter 14 feet may be assumed. Complete core meltdown of 218,000 pounds of UO_2 and 44,500 lbs of zircaloy would occupy about 473 cubic feet of the bottom volume. From the equation for the volume and surface area of a segment of a sphere of radius R (=7feet) (C1),

$$V = 1/3\pi h^2 (3R-h) \tag{3.17}$$

$$A = 2\pi rh \tag{3.18}$$

one can determine that a complete core meltdown would deposit debris of depth, h, 5.38 feet onto the hemispherical bottom, resulting in a heat transfer area of 236.6 square feet. Assuming an equivalent semi-infinite slab geometry of the same area, the describing differential equation is

$$\frac{\partial \theta}{\partial t} = \alpha \frac{\partial^2 \theta}{\partial x^2} , \tag{3.19}$$

where $\alpha = k/ c_p$

$$\theta = \frac{T_{(x)} - T_{initial}}{T_{interface} - T_{initial}}$$

X = distance into pressure vessel

For the idealized case of a semi-infinite solid initially at a uniform temperature of $T_{initial}$ (240°F for the pressure vessel) suddenly exposed at its surface to a fluid at constant temperatures, T_{fluid} (6000°F boiling point of UO_2), and a constant heat transfer coefficient, h, the transient temperatures are given by

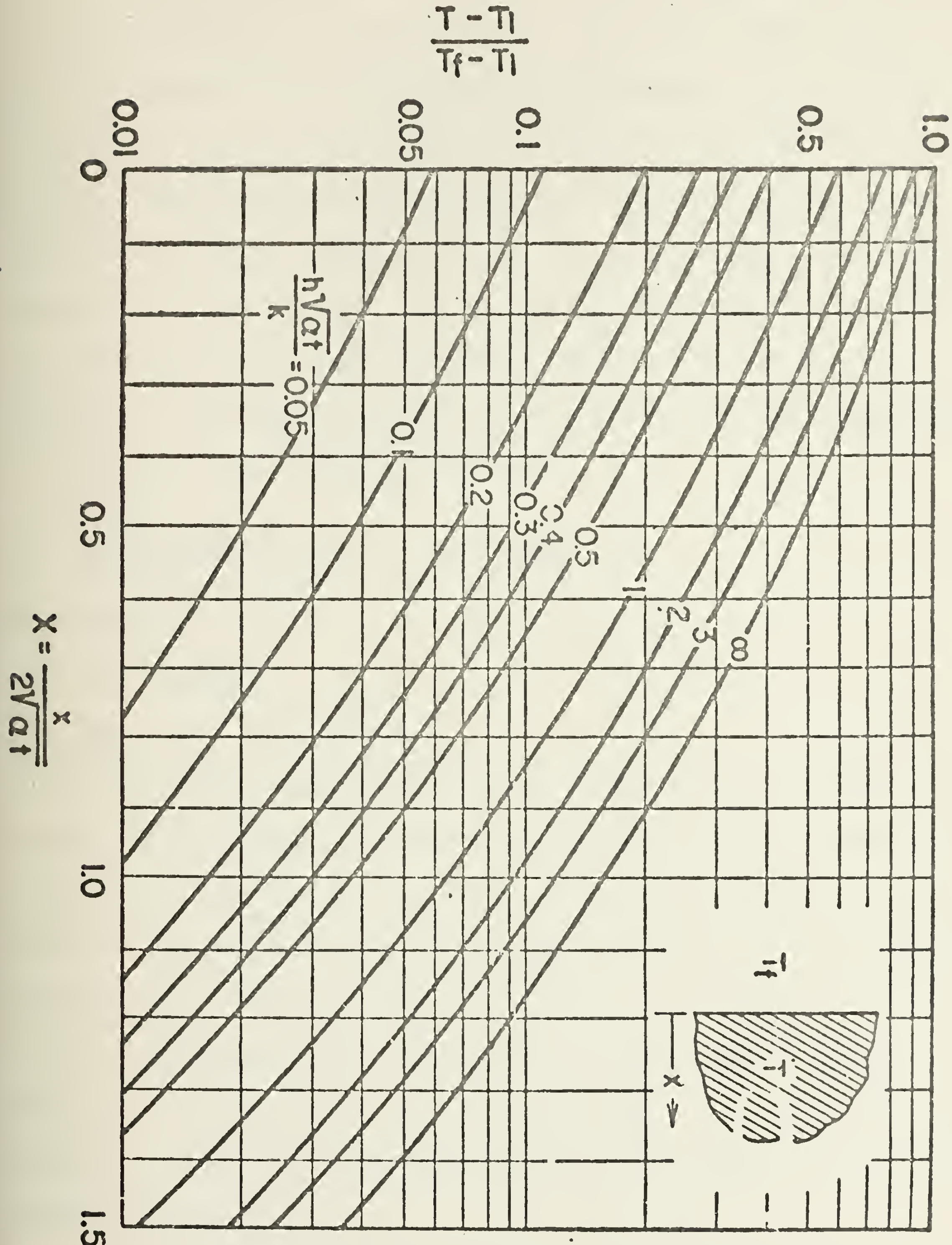
$$\frac{T_{init}-T}{T_{init}-T_{fluid}} = \left[\operatorname{erfc}\frac{x}{2\sqrt{\alpha t}} - \exp\left(\frac{xh}{k} + \frac{t}{(k/h)^2}\right)\operatorname{erfc}\left(\frac{x}{2\sqrt{\alpha t}} + \frac{\sqrt{\alpha t}}{k/h}\right)\right] \quad (3.20)$$

Solutions of Eq. 3.20 are plotted in Fig. 3.3 (A2). The appropriate value for the heat transfer coefficient is questionable; several reports (E3,Z1) have modeled the pressure vessel and core catcher sacrificial material meltthrough as a pure conduction problem, while the authors of BMI-1910 (M3) advocate use of the following correlation from Ref. M4 describing the cooling of a hot liquid by a cold plate facing downward:

$$Nu = 0.14\ (Gr\ Pr)^{1/3} \quad (3.21)$$

Recognizing the meltthrough situation to be one in which the molten matter is cooled at the bottom by virtue of melting the pressure vessel, BMI-1910 recommends using one-half the value of h obtained from this correlation; justification for this recommendation is given by comparing the resulting h with that obtained by Fontana (F2)--however, Fontana also considered a situation involving top surface cooling. As the molten material melts into the pressure vessel head, because of the curvature of the vessel some of the fuel will be cooled on the head at heights above other portions which have not yet begun to cool. Natural convection Bénard cells (M4) will thus be established, and the debris will cool by convective heat transfer. Utilizing the molten UO_2 properties from Appendix A, Eg. 3.21 results in a value of h equal to about 1360 BTU/hr-Ft^2-°F; taking one-half this value as recommended by the authors of BMI-1910 results in h=680 BTU/hr-Ft^2-°F. A pure conduction model in which it is assumed that the molten UO_2 sinks by displacing the

FIG. 3.3 TRANSIENT TEMPERATURE HISTORY
IN A SEMI-INFINITE SOLID



freshly melted steel reactor vessel material has been utilized by Zivi (Z1) in analyzing his core catcher proposal; the pure conduction model corresponds to a convective model with a value of h of around 50 BTU/hr-Ft^2-F. Solving Eq. 3.20 for the time, t, required for the melting front ($T=T_{m,steel}$) to reach point x (the reactor vessel thickness) and applying these values of the heat transfer coefficient to Eq. 3.20 results in meltthrough times of around 5 hours for an assumed h of 50 BTU/hr-Ft^2-°F and 2.62 hours for an h of 680 BTU/hr-Ft^2-°F. In the limit, for a perfectly conducting situation ($h=\infty$), the second term in Eq. 3.20 goes to zero, and one obtains

$$\frac{T-T_{initial}}{T_{fluid}-T_{initial}} = \operatorname{erfc} \frac{x}{2\sqrt{\alpha t}} \qquad (3.22)$$

from which

$$\operatorname{erf} \frac{x}{2\sqrt{\alpha t}} = 0.58$$

and $t = 2.35$ hours.

All these values <u>overestimate</u> the actual time required to meltthrough the pressure vessel, and, as such, are not conservative in terms of core catcher design. The overestimation occurs because in the actual case, the vessel material at its outside surface must transfer heat by radiation or natural convection to the containment air, while it has been assumed here that the vessel is semi-infinite in the x direction, thus, permitting transfer of heat by conduction through the steel at the vessel external boundary.

A more conservative time estimate is obtained by first writing a heat balance on the debris as

$$q(\text{decay heat})=q''_{top}A_{top}+hA_{bottom}(T-T_m)+C_pV_{debris}\rho\frac{dT}{dt}+\rho\dot{V}_v\lambda_v\ . \qquad (3.23)$$

If it is assumed the debris reaches the pressure vessel bottom 30 minutes after blowdown at the UO_2 boiling point of $6000^{\circ}F$, $\frac{dT}{dt}\leq 0$ and any excess heat generated (over that conducted to the pressure vessel) will either vaporize the debris, increasing the volume rate of vaporization term ($\dot{V}_v$), or increase the surface rate of radiative heat flux, q''_{top}. The heat transferred to the vessel, $hA\ (T_{melt}-T_{m,vessel})$, must first raise the vessel temperature from an assumed $240^{\circ}F$ to its melting point and then melt the steel. Thus,

$$hA\ (T_{melt}-T_{m,vessel})=\frac{dx}{dt}A_{bottom}\rho(C_p(T_{m,vessel}-T_{o,vessel})+\lambda_{f,vessel}), \qquad (3.24)$$

$$\text{or } t=\frac{XA\rho\ (C_p(T_m-T_o)+\lambda_f)}{hA\ (T_{melt}-T_m)}\ . \qquad (3.25)$$

It is noted that Eqs. 3.24 and 3.25 assume no heat is conducted away from the reactor pressure vessel bottom head into the other parts of the reactor vessel. Thus, the times for meltthrough calculated in this manner are conservative, assuming an adiabatic interaction between the core debris and the bottom head portion of the vessel only. Using values for the carbon steel vessel from Appendix A and for a vessel thickness of 14 inches, the time required for meltthrough is about 1.4 hours for an assumed h of 50 BTU/hr-Ft^2-°F and 0.1 hour for a heat transfer

coefficient of 680 BTU/hr-Ft^2-$^\circ$F (M3). The rate of heat transferred in either case is

$$q = hA\ (T_{debris} - T_m). \tag{3.26}$$

Using the total area covered by the debris of 236.6 square feet as calculated above, the heat transfer rate is 0.4×10^8 BTU/hr for h=50 BTU/hr-Ft^2-$^\circ$F and 5.4×10^8 BTU/hr for h=680 BTU/hr-Ft^2-$^\circ$F. The ANS Standard (Fig. 3.1) predicts a decay heat value of 1.86×10^8 BTU/hr 30 minutes after shutdown. Although the heat removal for an assumed heat transfer coefficient of 680 BTU/hr-FT^2-$^\circ$F exceeds the decay heat, the assumption that the debris temperature remains at 6000°F is still reasonable since this removal rate only occurs for 0.1 hour and since a decreased debris temperature also decreases the heat removal rate, while the heat generation rate remains constant.

3.6 CONTAINMENT MELTTHROUGH

As indicated in Chapter 2, the last significant barrier to the molten debris in its downward path on the presently configured floating plant is the approximately 3 foot thick concrete containment base plate under the in-core instrumentation keyway (see again Figs. 2.5 and 2.6).

A detailed analysis of the decomposition of concrete may be found in Ref. R1; Table 3.1 contains a summary of data germane to the decomposition of concrete found in that reference.

Table 3.1

CONCRETE DECOMPOSITION DATA

1. Composition of Concrete (weight %)

$CaCO_3$	60%
H_2O	20%
SiO_2	20%

2. Disintegration Temperature 900°F

3. Melting Temperatures

SiO_2	3100°F
CaO (From $CaCO_3$)	4400°F

4. Heat of Decomposition:

to heat to 4500°F and melt SiO_2 and CaO	2.05MW-sec/lbm concrete
to heat to 3100°F and melt SiO_2	1.35
to heat to 2200°F and spall concrete	1.10

5. CO_2 production 325 Ft^3 CO_2/Ft^3 concrete

Concrete is reported to partially disintegrate at about 900°F, but to maintain its retention properties until the $CaCO_3$ (limestone) begins decomposing between 1600°F and 2000°F to yield CO_2. At this point, because the density of the concrete particles is much lower than the molten fuel, particles of concrete, CO_2, and water vapor will rise rapidly through the liquid, either dissolving in the molten mass or forming a top layer. The surface of the concrete will spall and fragment continuously, exposing new concrete through the resulting churning process.

Because of the low thermal conductivity of concrete (k=0.64 BTU/hr-Ft-°F), the process of heat transfer between the molten debris and the concrete pad of the Containment Base

Plate (circle of diameter 14 feet) will very nearly be an adiabatic process; that is, very little heat will be conducted from the horizontal pad to the vertical concrete walls of the keyway. The total mass of concrete in the 3 foot pad is

$$M = \rho \text{ Volume,} \tag{3.27a}$$

$$= \frac{\rho \pi D^2}{4} \text{ (thickness)} \tag{3.27b}$$

$$= 6.46 \times 10^4 \text{ lbm.}$$

Using the heat of decomposition associated with raising the temperature of the concrete to 2200°F, $H=1.1 \frac{\text{MW-sec}}{\text{lbm concrete}}$ ($=1.04 \times 10^3$ BTU/lbm concrete), and the decay heat rate value at one hour (1.8×10^8 BTU/hr), the time to "meltthrough" the concrete is determined:

$$t = \frac{1.04 \times 10^3 \text{BTU/lbm} \times 6.46 \times 10^4 \text{lbm}}{1.8 \times 10^8 \frac{\text{BTU}}{\text{hr}}} \tag{3.28}$$

$$= 0.37 \text{ hours.}$$

If all the contained limestone decomposes to CO_2, a total of

$$V_{CO_2} = \frac{325 \text{ Ft}^3 \text{ CO}_2}{\text{Ft}^3 \text{ concrete}} \times \frac{\pi (14)^2}{4} \times 3 \tag{3.29}$$

$$= 1.5 \times 10^5 \text{ Ft}^3 \text{ CO}_2$$

will be generated. For a total containment volume of $1.25 \times 10^6 \text{Ft}^3$, this amount of CO_2 represents about 12% of the total volume, and hence a roughly equal percentage increase in pressure; which would be tolerable.

3.7 SUMMARY

This chapter has described in some detail the anticipated

sequence of events involved in reactor core meltdown. The following chapter will utilize this information, together with the offshore plant characteristics, to evaluate the feasibility of employing existing core catcher proposals on the barge system.

A conservative interpretation of the results of this chapter indicate that a proposed core catcher must be capable of assimilating about 250 tons of molten debris, potentially at the boiling point of UO_2 ($6000^{o}F$), as soon as one hour after blowdown. The decay heat rate at this time, determined using the ANS Standard, is 1.8×10^{8} BTU/hr, assuming no loss of volatile fission products from the debris.

Chapter 4

ANALYSIS OF PREVIOUS PROPOSALS

4.1 INTRODUCTION

Several core catcher concepts were introduced in Section 1.4. The presently conceived offshore, barge-mounted plant was described in Chapter 2, with particular emphasis on those systems and features related to the design and function of an installed core catcher, and the area and volume available for the core catcher design. The hypothetical core meltdown accident was then described in Chapter 3, indicating magnitudes of the decay heat rate to be accomodated, times of interaction, and an approximate sequence of events in the accident. In the present chapter previous core catcher proposals will be evaluated to assess their adaptability to the offshore plant design of Chapter 2.

4.2 ZIVI SACRIFICIAL UO_2 CONCEPT

In references Z1 and Z2 Zivi describes a "completely passive" barrier to penetration of the molten mass through the containment vessel. In neither reference is the total amount of UO_2 necessary to stop the meltthrough for all time calculated, nor is the UO_2 surface area required to remove the decay heat considered. Zivi analyzes two possible occurrences: first the case of the molten core floating on the sacrificial UO_2 layer, with no penetration; and then the more severe case of the molten core displacing and sinking into the freshly melted sacrificial UO_2.

Recognizing the thermal properties of UO_2 to be nearly the same in the solid and liquid states, Zivi models the case of the molten core floating on the sacrificial UO_2 as a semi-infinite slab having a sudden imposition of a temperature equal to the UO_2 boiling temperature on its surface. In this assumed situation the heat must be conducted through the previously melted sacrificial UO_2 to reach fresh (non-molten) UO_2, and the analysis results in a melting front penetration rate of 20 centimeters in 12 days. The describing heat conduction equation is

$$\frac{\partial \theta}{\partial t} = \alpha \frac{\partial^2 \theta}{\partial x^2} \tag{4.1}$$

where $$\theta = \frac{T(x) - T_o}{T_v - T_o}, \tag{4.2}$$

$$\alpha = \frac{k}{\rho C_p}$$

T_v= UO_2 boiling temperature, 6000°F

T_o= initial sacrificial UO_2 temperature, 80°F

$T(x)$= sacrificial temperature at distance x from surface.

Under the initial and boundary conditions

$\theta = 0$ all x, $t \leq 0$

$\theta = 1$ x=0, $t > 0$

$\theta = 0$ $x=\infty$, $t > 0$,

this equation has as its solution (B1),

$$\theta = 1 - \text{erf} \frac{x}{\sqrt{4\alpha t}}, \tag{4.3}$$

from which the heat flux into the sacrificial UO_2 is

$$q''\Big|_{x=0} = -K \frac{\partial T}{\partial x}\Big|_{x=0} = \frac{K}{\sqrt{\pi \alpha t}} (T_v - T_o). \tag{4.4}$$

Inserting the appropriate properties of UO_2 from Appendix A, one obtains a heat flux of 3.5×10^4 BTU/hr-ft^2, requiring (at a time of 1 hour) a surface area of about 5143 square feet to remove the decay heat at a rate of 1.8×10^8 BTU/hr (calculated from the ANS Standard + 20% in Chapter 1). This required area greatly exceeds the available compartment deck area under the reactor vessel and bounded by bulkheads 3 and 4 and F and G (see again Fig. 2.4), which totals 1774.25 square feet. A similar magnitude of required surface area is obtained for the case in which the molten core is assumed to displace the sacrificial UO_2.

Arnold (A1) has determined the total fission product decay heat released from a 3200 MWt core following infinite irradiation to be 9×10^7 MW-sec or about 8.53×10^{10} BTU. Fig. 4.1 illustrates the decay heat rate as a function of time after shutdown, and Fig. 4.2 shows the integral of this rate over time--the total energy released as a function of time. To absorb this heat completely by melting sacrificial UO_2 would require

$$M_{UO_2}(C_p(T_m - T_o) + \lambda_F) = 8.53 \times 10^{10} \text{ BTU}, \tag{4.5}$$

from which a total required mass of 70,000 tons of sacrificial UO_2 can be determined. This represents 37% of the total barge displacement as now envisioned. In addition to being completely cost prohibitive (about \$$10^9$ at current UO_2 prices), this quantity of UO_2 would require a complete re-design of the barge system.

It is not clear exactly what is being proposed by Zivi. Although utilizing the term "completely passive" in describing

FIG. 4.1 DECAY HEAT RATE vs. TIME AFTER SHUTDOWN

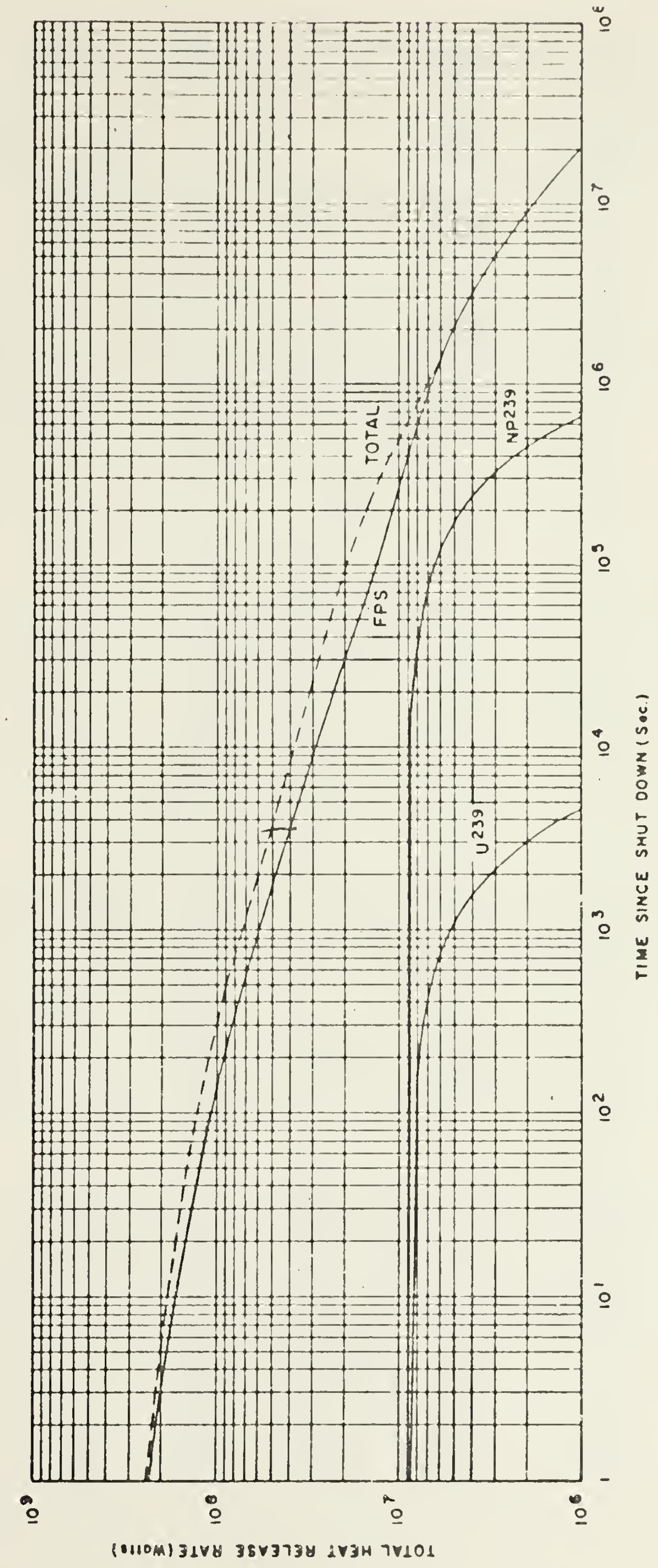
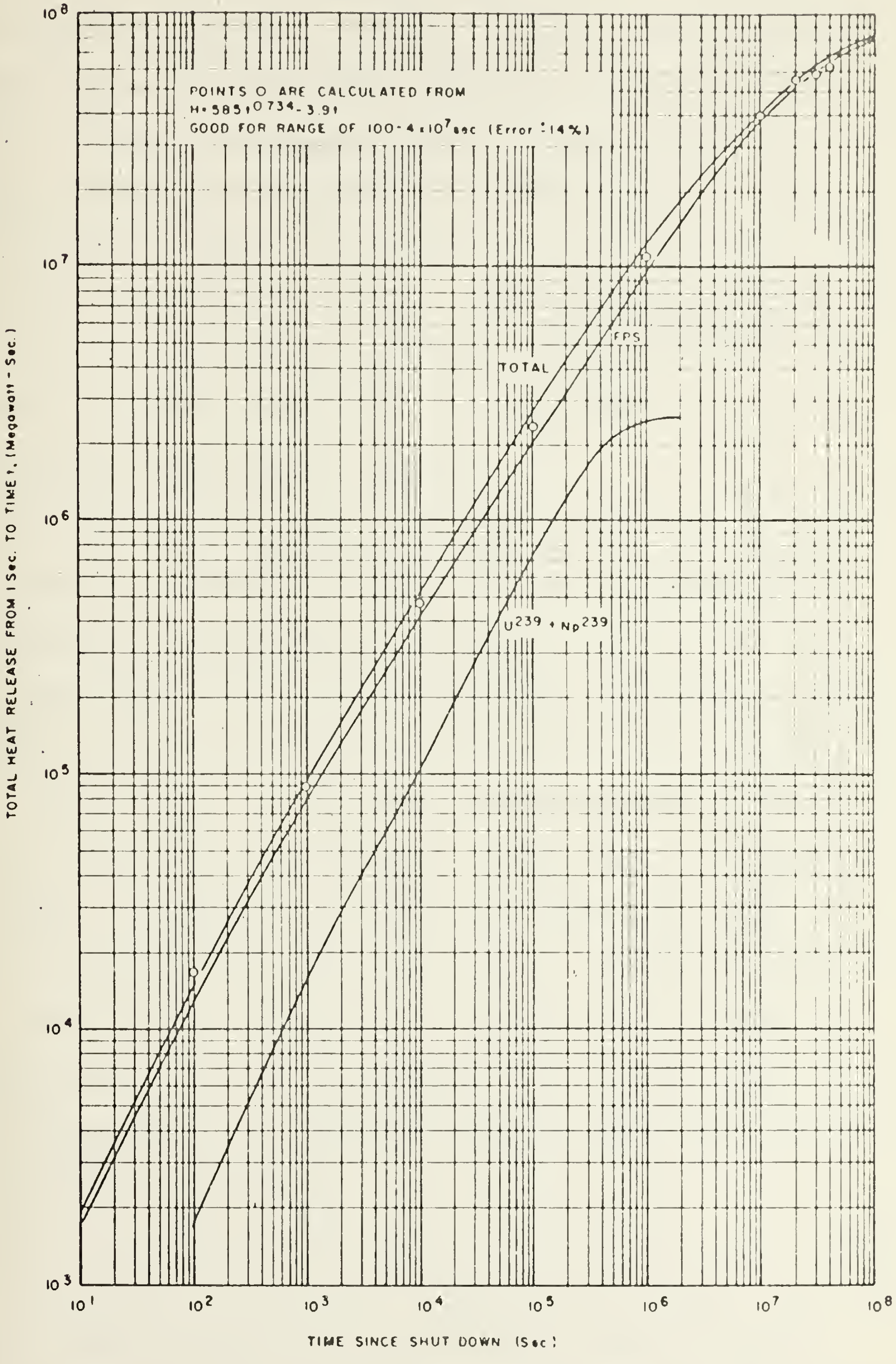


FIG. 4.2 TOTAL DECAY ENERGY RELEASED vs. TIME AFTER SHUTDOWN



his concept, he further states that this core catcher will detain the mass sufficiently long to prevent containment melt-through assuming "the heat generated by the fission products is removed from the containment vessel by a cooling system". Thus, the "completely passive system" still requires some form of active cooling to be economically and technically feasible, and this method is seen to be merely a variation of the concept proposed for Indian Point 2 (M3). As a completely passive system, the Zivi UO_2 sacrificial method is judged untenable for any use, but particularly unsuitable for barge use.

4.3 DOAN-CROWLEY LEAD SLURRY CONCEPT

This method closely parallels that of Zivi, as pointed out in Chapter 1; Doan and Crowley, however, recognize the impracticality of attempting to assimilate all the fission product decay heat by sacrificial melting, and require massive heat sinks in the form of water-cooled radiators to remove this energy. In Ref. D2, an iterative process is used to determine the mass of lead (M_1) and radiative surface area (A) required to establish equilibrium at time τ after LOCA for a given emissivity of lead (e). The lead pool is assumed to be at its boiling point ($T_{v,1}$), radiating to exposed radiator surfaces at temperature (T_a), at a rate equal to the decay heat rate $q(\tau)$ at time τ as given by the Untermeyer-Weills decay heat correlation. Thus at equilibrium time τ,

$$q(\tau) = \sigma e A(T_{v,1}^4 - T_a^4) \ . \qquad (4.6)$$

No credit is taken for any heat radiated until time τ, so that all decay energy released and the sensible heat of the UO_2 fuel is absorbed by heating and melting the mass of lead, M_1.

Thus,

$$\int_0^t q(t)dt + M_{UO_2}C_{p,UO_2}(T_{0,UO_2} - T_{v,1}) =$$

$$M_1C_{p,1}(T_{v,1}-T_{o,1}) + M_1 \quad , \tag{4.7}$$

where, T_{0,UO_2} = initial UO_2 temperature

$T_{o,1}$ = initial lead temperature.

Assuming the crucible to be built in the shape of a truncated cone, the authors found that for a top surface area (A) of 529 square feet, 8760 metric tons of lead and a crucible height of 72 feet (with a top surface to bottom surface ratio of 0.5) would be required; this size crucible is obviously unsuitable for barge use. However, for a top surface area of 1089 square feet, only 850 metric tons of lead and a crucible height of 3.6 feet are required. However, this assumes a lead-UO_2 equilibrium temperature of $T_{v,1}$(3158.6 ^oF), well below the freezing point of UO_2 (5072 ^oF), without including, as a heat source, the latent heat of fusion of the UO_2. This additional energy is

$$Q = M_{UO_2}\lambda_{F,UO_2}. \tag{4.8}$$

For 100 metric tons of UO_2 the additional energy to be absorbed by the lead is 2.38×10^7 BTU, and the additional lead required

$$M_1 = \frac{2.38 \times 10^7}{C_{p,1}(T_{v,1}-T_{o,1}) + \lambda_{F,1}} \tag{4.9}$$

$$= 84.6 \text{ metric tons}$$

for an assumed initial lead temperature of 80^{o}F. Additionally, as shown in Chapter 3, one should expect another 100-150 tons of structural metal to accompany the UO_2 fuel into the core catcher, possessing both sensible heat and heat of fusion which must be accomated by the lead. Therefore, it is seen that the

calculations in Ref. D2 result in a lower lead mass and core catcher height than would actually be required under completely conservative assumptions.

The calculations in Ref. D2 further assume perfect emission with no reflection back into the lead pool. For a given mass of lead, Doan and Crowley determine that 23 metric tons of lead per hour must be vaporized to accomodate a 10% reduction in radiant heat transfer. If the radiators were to utilize natural convection by boiling and exhausting at atmospheric pressure, the maximum heat flux that could be accomodated prior to reaching critical heat flux (CHF) is around 200,000 BTU/hr-ft^2 (E1), requiring a total radiator heat transfer area of around 750 square feet. For the radiators to have this large surface area, be located such that the radiation heat transfer is effective, be protected from the core debris when it falls, and still be located in the space available on the present barge design would be extremely difficult, especially when it is noted that the crucible requiring 850 metric tons of lead (Ref. D2 calculation) occupies over 60% of the available compartment deck area under the reactor vessel of 1774.25 square feet. Trying to compromise on upper pool surface area was shown previously to rapidly increase the quantity of lead required.

Finally, the cost of this design, while relatively inexpensive considering only the lead cost ($281,000 for 850 tons (D2)), must also include the unknown cost of the research and development, and then the design and manufacture, of the radiators and their associated piping. Total costing estimates

therefore cannot be made.

In summary, because of the uncertainty involved in the design and operation of the radiative heat transfer system, the difficulty of incorporating the system components into the available space, and the uncertainty of exactly how much lead is required, this proposal is judged non-ideal for barge use.

4.4 INDIAN POINT 2 CONCEPT

One of the first proposed solutions to the containment meltthrough problem, this method as submitted required water flooding to a height of 28 feet above the ceramic-lined crucible bottom. As shown in Chapter 2, the volume under the present reactor pressure vessel and inside the containment is now occupied by in-core instrumentation leads, and use of this space for a core catcher is not deemed feasible. The concept could be adapted to the open space below the containment, although the flooding height would be limited to 7.5 feet.

This proposal has been extensively analyzed by Refs. E3 and M3. The Ergen Report (E3) applies a conductive heat transfer model to the concept by assuming that a central plane exists in the molten debris which will heat to the vaporization temperature of UO_2. Assuming water cooling on the top of the debris pool and on the bottom of the crucible, the author concludes that equilibrium would be established in 30 minutes to 90 minutes with no significant refractory melting, and thus concludes that one should design for the steady state case. At the central plane, $T = T_{v,UO_2}$, $x = 0$, and $\frac{\partial T}{\partial x} = 0$, while at

the refractory surface, $T=T_{m,MgO}$ and $x=L$. The one-dimensional, steady-state heat transfer equation for constant thermal conductivity, k_{UO_2}, is given by

$$k\frac{d^2T}{dx^2} = -q'''. \tag{4.10}$$

Integrating once, one obtains

$$\frac{dt}{dx} = \frac{-q'''}{k}x + C_1 \quad , \tag{4.11}$$

and $C_1 = 0$ since $\frac{dt}{dx} = 0$ at $x = 0$. Another integration then yields

$$T(x) = \frac{-q'''x^2}{2k} + C_2 \ , \tag{4.12}$$

and at $x = L$, $T(x) = T_{m,MgO}$, such that

$$T_{m,MgO} = T_{v,UO_2} - \frac{q'''L^2}{2k} \ , \tag{4.13}$$

with

$$C_2 = T_{v,UO_2}. \tag{4.14}$$

From Eq. 4.13,

$$L = \left(\frac{2k(T_{v,UO_2} - T_{m,MgO})}{q'''}\right)^{1/2}. \tag{4.15}$$

Finally, the heat flux to the crucible is given by

$$q'' = -k\frac{dt}{dx}\bigg|_{x=L} \tag{4.16}$$

$$= q'''L \text{ (from Eq. 4.11)}$$

$$= \left(2kq'''(T_{v,UO_2} - T_{m,MgO})\right)^{1/2} \text{(from Eq. 4.15)} \tag{4.17}$$

Results obtained for assumed values of the parameters are given in Table 4.1. Note from this table that if no top surface cooling is provided, an area of 3422 square feet is required to remove the decay heat rate of 1.8×10^8 BTU/hr at one hour; top cooling is therefore needed to limit the required surface area.

Table 4.1

TID-24226 RESULTS FOR MgO CRUCIBLE

Parameters:

q''', BTU/ft^3-hr	300,000
k_{UO_2}, BTU/hr-ft-^oF	1.85
T_{v,UO_2}, ^oF	6300
T_{m,UO_2}, ^oF	5000
$T_{m,MgO}$, ^oF	3900
steel crucible thickness, in	1.0

Results:

q'', BTU/hr-ft^2	52,600
MgO thichness, in	2.7
max steel temp, ^oF	660

As pointed out in Chapter 3, the authors of BMI-1910 (M3) take issue with this purely conductive model, promoting a convective heat transfer mechanism with values of the heat transfer coefficient as large as 685 BTU/hr-ft-^{o}F, and thus predicting a crucible meltthrough in many cases. Additionally, although pure UO_2 has a higher melting point than MgO, and therefore appears to be a better liner candidate than MgO, a possible peritectic reaction between UO_2 and iron is reported in BMI-1910 which reduces the effective melting temperature of the UO_2 surface to 2800^{o}F. Similarly, the debris melting point could be reduced to 2800^{o}F by the reaction of core UO_2 and pressure vessel steel.

The required thickness of the refractory liner is determined by several, often conflicting, considerations; the liner must be thick enough to protect the steel crucible from melting, thin enough to allow for sufficient bottomside heat transfer, thick enough to prevent exceeding the critical heat flux at the bottom surface, and thick enough to withstand a potentially large mechanical shock as the core materials fall into the catcher. The thickness of a UO_2 refractory which can be melted is determined in BMI-1910 in a manner similar to the pressure vessel meltthrough discussion in Chapter 3. Starting from an initial debris boiling temperature, a heat balance on the debris gives:

$$q_{decay} = q_o A + hA(T-T_m) + (\rho CLA)_{debris}\frac{dT}{dt} \qquad (4.18)$$

where q_o = top surface heat flux $(0<q_o<360{,}000)$

T_m = debris and refractory melting point $(2800 < T_m < 5150)$
L = thickness of the debris
A = surface area

It is noted that $\frac{dT}{dt}$ must be either zero or negative; a positive value would indicate debris evaporation and not a temperature rise. Thus, the debris is either cooled or evaporates, depending upon values assumed for the surface heat removal rate, the debris and refractory melting point, and the convective heat transfer coefficient, h. Note that increasing the assumed value of h cools the debris faster, but also results in a higher rate of refractory penetration. Results in BMI-1910 indicate that the debris will cool ($\frac{dT}{dt} < 0$) for a slab diameter greater than 22 feet regardless of the value of h and T_m, if cooling at the top surface (q_o) is available at 360,000 BTU/hr-ft^2 (assumed CHF for atmospheric pool boiling of water). Even with this same value of q_o, for a diameter less than 15 feet the debris will continue to boil and the core catcher melt, if T_m is $\geq 5150°$F and h is $\leq$ 500BTU/hr-ft^2-$°$F.

For the case $\frac{dT}{dt} > 0$, a constant heat flux into the refractory will exist, and the thickness penetrated will be linearly proportional to time; if $\frac{dT}{dt} < 0$, the debris cools, and the penetration rate decreases with time until $q_o A$ is equal to the decay heat source, and the excess sensible debris heat is consumed by the melting process. For the worst case of $q''_{bottom} = 0$, all the heat transferred to the refractory at the rate $hA(T-T_m)$, is consumed in melting the refractory. Thus, for initial refractory temperature T_A,

$$hA(T-T_m) = \frac{dx}{dt} A\rho \left[C_f(T_m-T_A) + \lambda_F \right] \quad . \tag{4.19}$$

If the debris does not cool, T remains at T_{v,UO_2}, and the authors of BMI-1910 display plots showing depths of UO_2 melted versus time for varying values of the input parameter, h. For extreme values of h (500 BTU/hr-ft^2-^{o}F and 10 BTU/hr-ft^2-^{o}F), the analysis indicates that over 14 feet of UO_2 per day and about 1 foot of UO_2 per day, respectively, will melt. Again, it is noted that this assumes that a topside cooling rate equivalent to the decay heat source rate is available; for atmospheric pool boiling, El-Wakil (E3) references a CHF of 200,000 BTU/hr-ft^2-^{o}F, requiring a 900 square foot top surface area to remove 1.8 x 10^8 BTU/hr.

If the debris does cool, Eqs. 4.18 and 4.19 are solved to give a depth, x, of refractory penetrated as a function of time:

$$x = \frac{LC_p(T_{v,UO_2} - T_m) \left[1-\exp(-ht/\, LC_p) \right]}{C_p(T_m - T_A) + \lambda_F} \tag{4.20}$$

From this expression one can see that a melting depth equilibrium is reached which is <u>independent</u> of the assumed value of h, the heat transfer coefficient; the <u>rate</u> of penetration is determined by this value, but the rate is slow enough not to be of particular importance. The equilibrium depth for a UO_2 refractory is 4.4 inches for an assumed T_m of 5150^{o}F (no lowering of T_m due to potential metallurgical combinations) and 31.4 inches for a T_m of 2800^{o}F.

The authors of BMI-1910 conclude that there is insufficient knowledge of metallurgical, thermophysical, and heat-transfer

characteristics available to state with certainty that a thin ceramic-lined core catcher will work, and that, in fact, a conservative analysis concludes that molten cores cannot be contained in this manner. More importantly, the possibility of steam explosions and hydrogen generation from metal-water reactions must be considered (F3). This concept requires dropping the molten material directly into a pool of water, and as shown in Chapter 3, the resulting rapid generation of steam and its subsequent expansion could badly damage both the crucible and the barge structures. Additionally, even though the water surrounding the crucible has been shown to be effective in removing the decay heat for reasonable surface areas (and assuming no steam explosions), the absorbed heat must still be transferred from the water inside the barge to the environment. If a direct path exists from the space below the reactor vessel to the containment volume (see again Fig. 2.6), the installed ice condensers and containment spray system could be utilized as a heat sink; if not, additional barge modifications are required. Therefore, although the concept can be shown to stop the molten material, the possibility of a steam explosion (even without fragmentation leading to an area enhancement) is considered too risky to deem the concept advisable for implementation on a floating plant.

4.5 TONG IN-VESSEL CONCEPT

As previously described in Chapter 1, Tong considered the asymptotic post-meltdown configuration and demonstrated that by

totally submerging the reactor vessel in water, or by continuously spraying the vessel surfaces with water, the core could be retained on the vessel bottom head. His example (T2) for a 3000 MWt reactor requires a heat flux of 300,000 BTU/hr-ft^2 over sections of the bare vessel to absorb the one-hour decay heat rate.

However, the present Westinghouse reactor vessels, as described in Ref. O2, are permanently insulated on the cylindrical portion of the vessel below the refueling ledge with a reflective type insulation supported from the coolant nozzles; the vessel upper and lower heads are likewise insulated, but with removable insulation panels. The insulation consists of inner and outer sheets of type 304 stainless steel spaced a <u>minimum</u> of 3 inches apart with multilayer stainless steel foil as an insulating agent to prevent excessive heat loss from the vessel, as well as excessive thermal stress due to temperature gradients. Achievement of the heat flux magnitude of 300,000 BTU/hr-ft^2 (CHF) required for this concept is prevented by the presence of this insulation. The concept described in Ref. T2 should thus only be considered for <u>bare</u> reactor vessels. Another heat transfer mechanism relied upon in this concept is the boiling and condensing of steel inside the reactor vessel. There is no assurance that this cycle can occur, however, and that the steel will not plate out on the surfaces of the vessel, thereby stopping the cycle.

Although this concept is attractive because of its relative simplicity and low cost, it is not considered feasible in terms

of providing positive insurance against fission product release to the environment.

4.6 BMI-1910 SPREADER CONCEPT

While not presented in Ref. M3 as a specific design, the concept of spreading the debris to allow for more efficient cooling and resolidification is intuitively attractive. Several problem areas are discussed in the proposal including: (1) large diameter slabs are required to avoid the long term presence of molten UO_2; (2) the debris may "dam" due to crust formation and not spread; and, (3) the possibility of steam explosions again exists. The type of crucible employed to hold the debris is not discussed.

Assuming steady-state conductive heat transfer in the slab and water cooling on one side of the slab, the resulting slab size required for resolidification is plotted in BMI-1910 versus center plane temperature, to allow a range of assumed solidifying temperatures to be checked. For an assumed $T_{m,debris}$ of $5072^{\circ}F$, the required slab diameter is around 60 feet, and the thickness is limited to about 2.5 inches, assuming no volatile fission product loss from the slab; if the decay heat rate is assumed reduced by one-half due to the loss of volatile fission products, a diameter of about 47 feet is required.

The concept of spreading the core to increase heat transfer is acceptable for barge use. Because of the magnitude of the required diameters, a pure spreading design utilizing only top surface water cooling is not acceptable; however, if employed

as an integral precept of a design utilizing more (or better) heat transfer mechanisms, spreading the debris is a useful proposal.

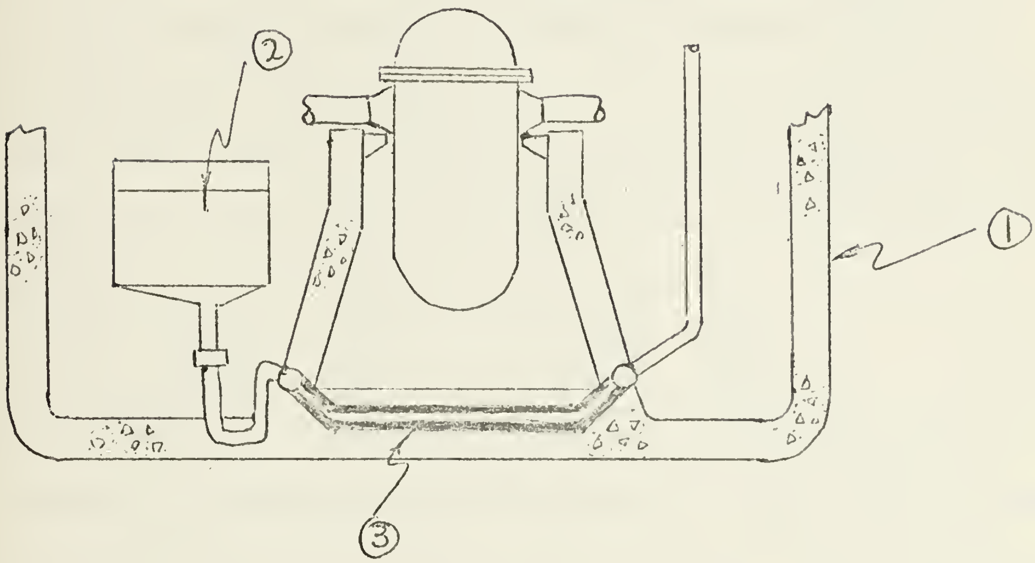
4.7 WEST-FLETCHER WATER-COOLED CRUCIBLE

The primary innovation of this patented invention is the use of water filled tubes actuated automatically and depending only upon gravity induced flow and natural convection for proper operation. By removing free water from the crucible, the steam explosion problem is circumvented, and by removing the dependency upon external power or signals, the concept is truely passive, requiring no external action for its operation.

In the preliminary discription of the invention, the authors rely on gravity and the low viscosity of the UO_2 to spread the molten debris over the crucible surface. However, as indicated in the BMI-1910 Spreader Concept, a dam of solid debris could form to prevent complete dispersal. A variation suggested in the patent disclosure to provide a layer of lead on top of the tubes, could help to ensure spreading over the surface.

Insufficient information is provided in the actual disclosure of the invention for a detailed analysis of the specific concept: no dimensions, materials, flow rates, or other pertinent data are discussed. The concept of the invention, shown in Fig. 4.3, is adaptable in principle to barge use. A straight run of pipe opening to the sea at the 0' elevation of the barge (32 foot submergence depth) could replace the reservoir shown

FIG. 4.3 WEST-FLETCHER CATCHER CONCEPT



(1) Containment Vessel
(2) Reservoir
(3) Welded-pipe Bed

in Fig 4.3. The inlet pipe could then feed an inlet plenum as described in this invention, which in turn could supply water to the multi-tube crucible, terminating in an outlet plenum and outlet standpipe, sized to minimize the resistance to steam flow out of the crucible, and extending above the water surface. If desired, the piping could be isolated from sea pressure during normal operation by including remotely operated gate valves in the line; however, this would require an operator or sensing-device action to initiate the flow, removing a degree of passiveness from the concept. Assuming the sea water is heated to saturation (100% quality) at 20 psia in the crucible by the decay heat source, an order-of-magnitude estimate of the required mass flow rate may be determined by an application of the steady-flow energy equation (see Fig. 4.4),

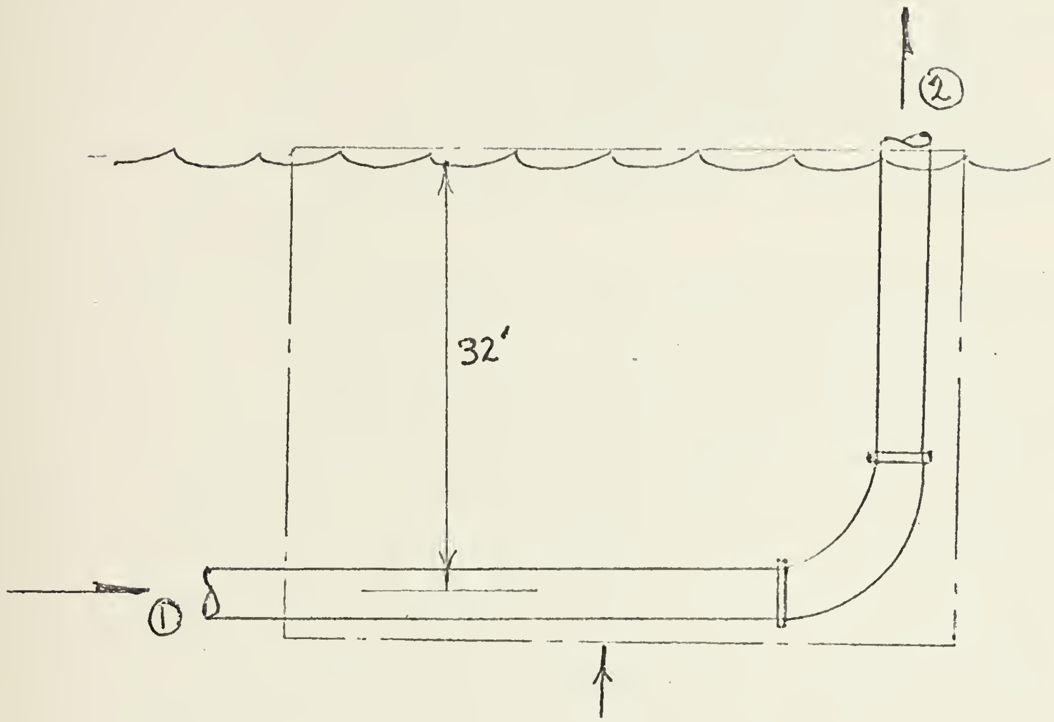
$$q = \dot{m}\left[\left(h + \frac{V^2}{2g_cJ} + \frac{gZ}{g_cJ}\right) - \left(h + \frac{V^2}{2g_cJ} + \frac{gZ}{g_cJ}\right)\right]. \qquad (4.23)$$

The actual velocities, V_1 and V_2, must be determined by an iterative process, considering the net driving head, the two-phase friction and acceleration losses, and the pipe sizes; however, it has been shown that the velocity head and elevation head are both negligible compared to the energy manifested in the enthalpy change. Thus, for a conservative, order-of-magnitude approximation to the mass flow rate required, Eq. 4.23 reduces to

$$q = \dot{m}\,(h_2 - h_1) \qquad (4.24)$$

and, $\dot{m} = 1.6 \times 10^5$ lbm/hr, for the decay heat rate value at one hour after shutdown (1.8×10^8 BTU/hr) and for the sea water conditions discussed above and illustrated in Fig. 4.4.

FIG. 4.4 SCHEMATIC CONFIGURATION FOR CONVECTIVE FLOW



$P_{①} = 28.9$ psia
$*T_{①} = 70\ ^{o}F$
$h_{①} = 38$ BTU/lbm
$\rho_{①} = 62.3\ lbm/ft^{3}$

$P_{②} = 14.7$ psia

$*h_{②} = 1156.4$ BTU/lbm
$\bar{\rho}_{②} = 0.05\ lbm/ft^{3}$

* assumed

The driving head available will depend upon the average density in the outlet leg, $\bar{\rho}_{steam}$, and is given by

$$\Delta p_{driving\ head} = (\rho_{water} - \bar{\rho}_{steam}) \frac{gZ}{g_c} . \qquad (4.25)$$

For order-of-magnitude considerations, assume $\bar{\rho}_{steam}$ is again the density of saturated steam at 20 psia, yielding

$$\Delta p_{driving\ head} = (62.3 - 0.05)\ 32 = 13.8 \text{ psi.}$$

It is seen that because of the relative magnitude of the density of the water and steam, the driving head will essentially be equal to the head of water on the inlet pipe, and the column of steam can be neglected.

An inherent risk involved in this design is the potential meltthrough of one of the crucible tubes resulting in the possible release of fission products to the environment via the outlet steam pipe. Uncertainties which could lead to a tube melt-through include uneven debris spreading, with a resultant excessive heat flux over a particular pipe section, decreased flow due to fouling of a tube, flow maldistribution due to pressure pulses, and unequal composition or concentration of the fission products within the debris, again resulting in excessive heat flux over effected areas. From the Naval Architect's viewpoint, every additional pipe subjected to submergence pressure represents a potential flooding casualty from sea chest failure, joint failure, or pipe rupture. Therefore, although this method provides a passive means for obtaining cooling water, a variation which could circumvent the risks of internal flooding and fission product release, while avoiding the complexity in the

form of a welded bed of piping, should be considered.

By utilizing the installed Containment Spray System as a condenser and the water from the primary coolant system and spray systems, no piping external to the barge hull would be required. A steam flow path will either exist naturally from the space beneath the reactor vessel up through the vessel and into the containment volume via the pipe rupture which initiated LOCA, or could easily be provided with internal ducting. The return path for cooled water into the crucible space could likewise be provided by internal piping, or natural gravity drain from the containment volume. Since it has been shown in Section 4.2 that some form of external cooling is required for a feasible core catcher, a modified West-Fletcher concept of a natural convection/gravity flow system may be utilized on the barge-mounted plant.

4.8 JANSEN COMBINATION CATCHER CONCEPT

As indicated in Chapter 1, Jansen has employed several concepts in his fast reactor core catcher proposal (J3). To disperse the molten core over a large area, preventing recombination into a critical mass and diluting the volumetric heat source, a ceramic oxide (preferably basalt), with a low melting point and in which the fuel is soluble, forms the first layer in the catcher. The maximum temperature in the system is then lowered as the fuel temperature is reduced to the ceramic melting point through the absorption of the generated heat into the latent heat of fusion of the ceramic, assuming sufficient ceramic is provided. By spreading the resulting fluid mass, an

increased heat transfer area for convection and radiation is obtained. Next, a layer of UO_2 is employed as a slow-melting protective barrier, as discussed in the Zivi proposal. Fire bricks or other insulating material then protect the steel crucible (or the steel containment vessel) against excessive temperatures. Finally, heat is removed from the containment vessel by cooling water passing through pipes embedded in the structural concrete of the vessel wall, although the patent disclosure states that the cooling pipes could be eliminated if the containment vessel could be made "larger". No supplementary method of heat removal is discussed if the pipes are removed, so again reliance is placed upon the gradual melting of the sacrificial UO_2 and radiation for decay heat removal.

For an assumed 500 MWt fast reactor core, calculations described in the patent (J3) indicated that for a four-foot layer of basalt brick, a four-foot layer of fire brick, and cooling pipes maintained at 150.8^{o}F, the pool reaches a maximum temperature of 2588^{o}F, 65 hours after meltthrough of the reactor vessel. The "Summary of the Invention" paragraph in the patent describes an entirely different configuration of three feet of basalt bricks, six inches of depleted uranium, and a foot of thermal insulation, for which no example calculation is given.

As shown in Section 4.2, an external cooling source is required for cooling a 3400 MWt core, since the patent speaks to a much smaller, 500 MWt, fast reactor. Representing a combination of the BMI-1910 Spreader Concept, the Zivi Sacrificial Concept, and the West-Fletcher Water-Cooled Crucible

Concept, this proposal is rejected for barge use because of the combination of disadvantages cited in Sections 4.2, 4.6, and 4.7. A sophisticated concept such as Jansen's, employing a sufficient number of barriers and with appropriate heat removal provisions, can conceptually stop a core meltdown; however, the complexity and cost involved in such an endeavor work against its use.

4.9 SUMMARY

An analysis of the adaptability of existing core catcher proposals to an offshore, barge-mounted plant design has been presented. All of the concepts were found to provide some degree of additional containment protection but were not considered readily adaptable to the offshore plant unless major design changes were made to the barge configuration or to the catcher proposal to achieve weight, size, or cost compatibility. The existing proposals and a brief summary of the major advantages/disadvantages are presented in Table 4.2.

The following chapter will investigate an alternative concept; a relatively cheap, light-weight, semi-passive design incorporating a heat absorbing pebble bed. This concept combines many of the better features of previous proposals, while eliminating the most significant faults.

Table 4.2

SUMMARY OF CORE CATCHER PROPOSALS, ADVANTAGES/DISADVANTAGES

CONCEPT	COMMENTS
Zivi Sacrificial UO_2	Requires 70,000 tons of UO_2 to passively contain core; excessive cost, weight, volume.
Doan-Crowley Lead Slurry Concept	Excessive weight; location of required radiators questionable; uncertainties associated with mass of lead required, design/operation of radiators.
Indian Point 2	Uncertainties associated with heat transfer mechanism result in uncertainties concerning amount of ceramic required as a liner; further metallurgical and thermophysical uncertainties; potential steam explosion.
Tong In-Vessel	Reactor vessel insulation prevents achieving required heat flux to prevent meltthrough; uncertainty in the required steel evaporation/condensation cycle.
West-Fletcher Water-Cooled Crucible	Advantageous to utilize natural convection/gravity flow on barge; piping external to barge presents potential path for escape of fission products; sea water piping internal to barge represents potential flooding casualty; complex welded pipe bed required.
Jansen Combination Catcher	Excessive complexity, cost, volume, and weight required.

Chapter 5

THE GRAPHITE PEBBLE BED CORE CATCHER

5.1 FOREWORD

Several existing core catcher proposals were reviewed and analyzed in Chapter 4 for possible use on the presently envisioned barge-mounted nuclear plants; none was found to be totally acceptable. In particular, many of the concepts require use of large amounts of high density materials, while for barge use smaller quantities of low density materials would be particularly desirable. One low density material, graphite, incorporates many additional desirable physical properties--high thermal conductivity, heat capacity, compressive strength, and melting point--and is therefore an excellent candidate material for core catcher applications. This chapter introduces the graphite pebble bed concept, summarizes correlations applicable to functional evaluation, and then develops analytical and experimental verification of the projected performance. Physical property values and nomenclature used are defined in Appendices A and B.

5.2 MOTIVATION FOR, AND DESCRIPTION OF, THE PEBBLE BED CONCEPT

The selection of graphite as a potential core catcher material was impelled by many favorable considerations including its thermal and mechanical properties, as discussed in Section 5.3. The configuration in which to employ the graphite, as a pebble bed installed in the compartment under the reactor vessel, was dictated by many of the shortcomings and disadvantages of

existing catcher proposals, as discussed in Chapter 4.

It is desirable to contain the meltdown products in solid form rather than in a molten state, if possible. In this way the debris can be rigidly maintained in a fixed position, minimizing the possibility of undesirable chemical or physical interactions with the potential core catcher material and with the surroundings. In addition, any gaseous or volatile fission products still present will be immobilized. The retention of the meltdown products in a solid state requires the rapid quenching of a large quantity of molten debris, followed by long term removal of fission product decay heat at a rate sufficient to prevent re-melting. The relative magnitude of these two heat loads is as follows:

<u>quenching process</u> - for 250 tons debris @ $T_{init} = 6000^{o}F$ with $T_m = 5072^{o}F$:

$$\text{Total Heat} = \text{Sensible Heat} + \text{Heat of Fusion} \tag{5.1}$$

$$Qq = M(Cp\,(T_{init} - T_m) + \lambda_f) \tag{5.1a}$$

$$= 9.57 \times 10^{7}\ \text{BTU.}$$

<u>long term process</u> - for a typical 1000 MWe plant, from Arnold (A1) and Fig. 4.1:

$$\text{Total Heat} = \int_0^{\infty} (\text{Heat Rate})\,dt - \int_0^{3600} (\text{Heat Rate})\,dt \tag{5.2}$$

$$Q_{lt} = 8.51 \times 10^{10}\ \text{BTU.}$$

The quenching process thus involves the removal of a relatively small quantity of energy, but at a high rate of removal, while the long term process involves a much larger quantity of total

energy (enough to reheat the original mass to its boiling point 1000 times over), but at a lower rate (1.8×10^8 BTU/hr @ 1 hour decaying to 3.4×10^6 BTU/hr @ 231 days--see Fig. 4.2). Quenching the initial molten charge in water (or any liquid for that matter) involves the potential of steam explosions; the graphite bed thus provides a quenching medium (high thermal conductivity, high heat capacity), while circumventing the steam explosion problem. In addition, water from the Primary System, the Containment Spray System, and the Ice Condenser System which could otherwise flood the core catcher compartment will drain through the bed; the level in the bed can be controlled as necessary by the installed Drain System as described in Chapter 2. That chapter indicated that the total volume of water which could potentially drain into the core catcher is about 101,600 cubic feet (12,600 Ft^3 primary system + 42,000 Ft^3 Ice Condenser + 47,000 Ft^3 Containment Spray). The total volume per cubic foot of depth available in the watertight area (bounded by bulkheads 3 and 4 and F and H in Fig. 2.4) is 2483.95 Ft^3/Ft; this figure includes 709.7 Ft^3/Ft for the catcher compartment (for a void fraction $\epsilon = 0.4$) and 1774.25 Ft^3/Ft for the adjacent compartment. The total depth available in these compartments is 7.5 feet. Thus, if <u>all</u> the available water drained into the core catcher, a prohibitive depth of 40.9 feet would be reached. The installed Drain System must be activated by level probes in the adjacent compartment to maintain an acceptable level in the catcher. Once the debris is quenched and solidified, it may be desirable to intentionally flood the bed to provide

bottom cooling. The analysis of the Zivi Sacrificial UO_2 core catcher (Z2) in Chapter 4, and the energy magnitude seen from Eq. 5.2, indicate that some form of <u>active</u> cooling is needed to remove the long term decay heat; in this respect the graphite pebble bed design does not differ from any other potential concept, and a proposed method for long-term heat removal is described below.

Because of the division of the core catcher compartment under the reactor vessel into four spaces by the $7\frac{1}{2}$ foot deep girders, use of a crucible, per se, in this area is prohibted. The graphite pebbles could easily be poured into these spaces, however, and the barge bottom protected by a layer of refractory material such as a graphite liner. Thus the configuration of a pebble bed design is compatible with the existing spatial layout.

By suspending the solid debris in the open lattice-work of the graphite bed, cooling water can completely surround the debris, increasing the available heat transfer area over a crucible-slab geometry, some versions of which may have cooling available only at the top surface. Thus the pebble bed design provides a multiplicity of coolant channels without the concern of maintaining the integrity of the post-impact catcher structure as in solid designs.

Finally, the pebble bed design affords impact protection for the barge bottom plating, which would not exist with a solid crucible-type core catcher. Much of the dynamic loading of the falling debris will be absorbed as the pebbles squeeze

into previously voided areas. The potential advantages of a pebble-bed core catcher are summarized in Table 5.1.

Given the basic concept of a graphite pebble bed, there are many ways in which it may be implemented. As previously pointed out, the transverse girders which divide the catcher compartment lead one to consider pouring the graphite bed into place, allowing the pebbles to fall into the existing contours without installing another crucible-type container. Since the bed penetration rate of the molten debris will be seen (in Section 5.4) to vary directly as the square of the bed particle diameter, it might appear desirable to utilize extremely small particles, even a graphite powder. In addition to decreasing the bed penetration rate, this concept could conceivable provide a "wicking" action similiar to that employed in heat pipes, thus greatly enhancing the conduction of heat away from the debris. Unfortunately, because it is difficult to initiate the capillary action in a heat pipe, the concept may not work without an elaborate priming system being employed, and even then, the system reliability would be questionable. Additionally, dust particles in air are potentially explosive; a fine dust bed would also inhibit the flow (and drainage) of water (and steam) around the frozen debris, reducing the available heat transfer. The small-particle powder may also form a graphite "mud" or paste with water, which could mobilize the bed, causing the powder to slip out from under the heavier UO_2, and allow the debris to sink to the bottom of the bed. A related consideration would be the potential steam scouring which could occur as high velocity

Table 5.1

PEBBLE-BED CORE CATCHER ADVANTAGES

1. Voided channels allow system water to drain through the bed, reducing the potential for a steam explosion.

2. Bed is configurationally compatible with the existing barge design, requiring no changes in the barge strength member design.

3. Bed requires no crucible or other container, avoiding the expense of designing and installing another structure.

4. Open lattice-work allows cooling water to surround the frozen debris, increasing the heat transfer area and thus the probability of retaining the meltdown products.

5. Bed particles compress into previously voided areas upon loading, providing impact protection from the falling debris for the barge bottom.

steam generated by contact of water with the debris from underneath impinges on the small particles, fluidizing the bed and again allowing the debris to sink deeper into the bed. Investigations (L2,Z3) of the limiting velocity before a bed is fluidized have resulted in the relationship

$$V_{limit} = \frac{0.005 g d_p^2 (\rho_p - \rho_{H_2O})}{\mu_{H_2O}(1-\epsilon)} \tag{5.3}$$

$$= 1.503 \times 10^3 \, d_p^2 \text{ ft/sec,}$$

for d_p = particle diameter in inches
ϵ = 0.40
ρ_p = 110 lbm/ft^3
ρ_{H_2O} = 0.026 lbm/ft^3 (sat steam @ 10 psig)
μ_{H_2O} = 0.0314 lbm/hr-ft (sat steam @ 10 psig).

Thus, for a small-particle powder size of 50 mesh (d_p = 0.0005 inches) the limiting steam velocity is only 0.000376 ft/sec, while for a larger particle size (d_p = 0.5 inch) the limiting steam velocity is 375.8 ft/sec (about one-fourth of sonic velocity), and the bed is much less likely to scour while venting steam. The potential disadvantages of a powder-bed core catcher are summarized in Table 5.2.

It is shown in Section 5.5 that a graphite bed thickness of 7.5 feet (the depth of the core catcher compartment) is theoretically more than sufficient (including an ample safety margin) to stop the molten debris from contacting the barge bottom; however, without further detailed experimental simulation it is not possible to rule out a portion of the debris funneling through the bed (although this is extremely unlikely for a 7.5 foot bed of 0.5 inch particles--i.e., a bed depth of a minimum of 180 particles)--thus a refractory liner for the

Table 5.2
POWDER-BED CORE CATCHER DISADVANTAGES

1. Heat pipe capillary-type action is difficult to initiate, reducing the system reliability and/or requiring elaborate priming techniques.

2. Dust particles in air are potentially explosive.

3. Powder would inhibit the flow of cooling water around the debris, reducing the available heat transfer.

4. Powder may form mud with water, slipping out from under the heavier UO_2.

5. Small particle diameter powder is easily fluidized by escaping steam, providing a tunneling effect to allow debris to fall through the bed.

bottom is prudent. Because the liner should possess many of the same physical properties as desired for the bed material (except that a low thermal conductivity is now required to minimize heat conduction to the barge-bottom steel), graphite is again a preferred material; the thermal conductivity of graphite can be adjusted from about 1.0 BTU/hr-Ft-^{o}F to 100 BTU/hr-Ft-^{o}F by varying the porosity and/or impregnating material (B5). Other candidate materials, such as MgO, UO_2, SiC, ZrO_2, Al_2O_3, all are more dense, more expensive, and melt at temperatures below the melting point of UO_2. For this weight- and volume— limited application, both the BTU/lbm ($Cp \Delta T$) and the BTU/Ft^3 ($\rho Cp \Delta T$) are important for both the liner and the bed material, where $\Delta T = Tm - T_{init}$. Thus, the ideal liner material is one of low cost, low thermal conductivity, high heat capacity per unit weight, and high heat capacity per unit volume. The ideal bed material would possess these same properties but with a high thermal conductivity, enabling it to quickly remove heat from the debris. Table 5.3(V1, V2, C1, Appendix A) summarizes these properties for the candidate materials, indicating graphite to be the superior choice. A standard 9 inch brick is 9" long x 4" wide x 2.5" thick, requiring about 6300 bricks to cover the catcher compartment deck. For MgO this would involve a cost of $33,138 and a total weight of 31.7 tons; for the same thickness of graphite a cost of $20,330 and a weight of 20.3 tons is required.

Several concepts can be envisioned to provide active cooling to the core catcher after the debris has solidified. Sea

Table 5.3

CANDIDATE CORE CATCHER MATERIALS

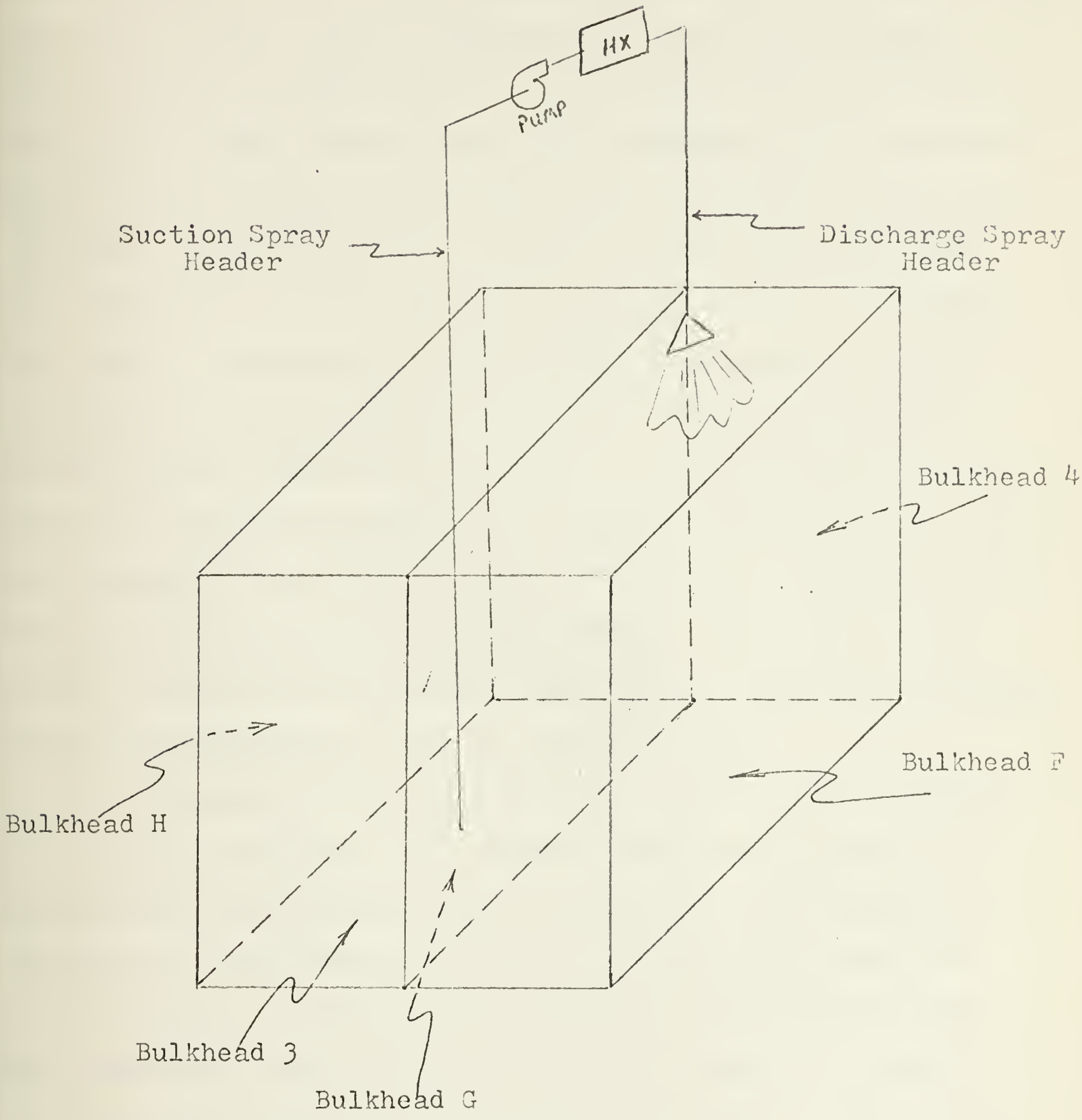
Material	T_m (°F)	$C_p T$ (BTU/lbm)	$C_p T$ (BTU/FT^3)	k (BTU/hr-ft-F)	Cost* ($/9 in)
Al_2O_3	3660	1.3×10^3	2.5×10^5	1.3	5.87
ZrO_2	4710	0.9×10^3	2.4×10^5	0.6	21.64
MgO	3900	1.6×10^3	2.7×10^5	1.5	5.26
SiC	4082	1.4×10^3	2.3×10^5	6.9	3.95
UO_2	5072	0.5×10^3	3.0×10^5	1.85	312.50
Graphite (bed)	6700	2.8×10^3	3.1×10^5	100.0	2.86
Graphite (liner)	6700	2.8×10^3	3.1×10^5	1.0	2.86

* Cost in $ per standard 9 inch brick

water could be used in a manner similar to that proposed by West and Fletcher (W4) and discussed in Chapter 4, as pointed out in that chapter. However, this method provides a potential escape route for fission products into the environment. A relatively simple cooling method could be provided by adding to the existing Containment Spray System. In the hypothetical meltdown, after the molten debris breaches the concrete Containment Base Plate and falls into the core catcher, the spray pumps (which had been drawing water from the reactor sump formed by the Base Plate) must shift suction to another water source. By providing (remotely operated) suction headers into the compartment adjacent to the catcher compartment (bounded by bulkheads 3, 4, G, H, see Fig. 5.1) the spray pumps could remove heated water from this area, pump it through the Containment Spray heat exchangers, and spray it back into the core catcher compartment and pebble bed to provide cooling for the frozen debris as shown in Fig. 5.1. A coolant circulation path through the pebble bed, into the adjacent compartment, into the spray pumps, through the heat exchangers, and back into the pebble bed is therefore established by the addition of the suction header in the adjacent compartment and the spray header in the catcher compartment. Note that these added headers represent penetrations in the existing containment and must therefore be provided with isolation valves in compliance with the AEC General Design Criteria (01).

Another method which could provide complete or partial cooling to the debris once solidified would be to flood the

FIG. 5.1 THE GRAPHITE PEBBLE BED CONCEPT



*Pebble Bed Bounded by Bulkheads 3,4,F, and G.

bed via the adjacent compartment, allowing the water level to reach a height where boiling begins, and cooling the debris with the steam exiting from the bed into the containment volume. For a stagnant graphite bed (ϵ = 0.4) whose voids are filled with air or steam (except those voids occupied by the solidified debris) Ref. 24 indicates an effective bed thermal conductivity of about 0.25 BTU/hr-Ft-^{o}F. If all the one-hour decay heat were conducted down into the bed at a rate of 1.8 x 10^{8} BTU/hr, the graphite temperature reaches 212^{o}F very close to the debris (a nominal, but meaningless, 8.1 x 10^{-5} inches away for an assumed debris temperature of 5072^{o}F). Thus this method of cooling would undoubtedly result in a situation where violent steam/water chugging would occur due to intermittent contact by water, followed by venting of the flashed steam. It is unresolved whether the bed can accommodate this mode of operation, although favorable considerations will be described later.

To summarize, it is proposed that a pebble bed, composed of (nearly) spherical $\frac{1}{2}$ inch diameter graphite pellets, be established in the voided space below the 7.5 foot waterline (beneath the Containment Base Plate) and directly under the reactor vessel on the presently designed barge-mounted plant. The compartment deck is to be lined with a layer of graphite to protect the vessel steel plating. An active overhead spray cooling system, utilizing the pre-existing Containment Spray pumps, piping, and heat exchangers is to provide long-term heat removal, perhaps in conjunction with backflooding the pebble

bed from below via the compartment adjacent to the catcher compartment.

The total weight of this proposed core catcher is 408.15 long tons, including both the pebble bed with a void fraction of 0.4 (7984 ft^3 of graphite) and the 2.5 inch liner (328 ft^3). From the information of Chapter 2 this results in an increased draft of 1.05 inches and an additional bottom-plate loading of 3.58 lbf/in^2, compared to the upward buoyancy force of 14.4 lbf/in^2. Only very minor (if any) structural strength additions need be made to accomodate this small incremental change. Fig. 2.2 indicates this compartment's center of gravity to be 36.5 feet from the barge midships ( )( ) and 18.9 feet from the barge centerline. The resulting moments yield an imperceptibble heeling angle of 0.008° and trim angle of 0.00025° by the bow. If desired, these moments may be easily compensated for by the installed trim system on the barge. For example, the trim angle can be removed by shifting only 23.3 tons of water (each) from the two forward trim tanks to the two after trim tanks. At $0.50 per pound of graphite (V1) a materials cost of $457,140 is obtained; an industrial consultant has indicated that the incremental cost for obtaining the product in pebble form would be "negligible" (V1).

5.3 PERTINENT GRAPHITE PROPERTIES

As described above, the first requirement of the pebble bed is to quench the meltdown products in order to rapidly freeze the debris and allow subsequent water cooling without the steam

explosion hazard. The low thermal expansion of graphite in combination with its high thermal conductivity and strength make graphite the most resistant to thermal shock of any known material (C5), and thus ideal for quenching.

Graphite has been determined to be non-wetting and non-reactive with molten metals and molten oxides (B2). In particular, experiments at the Oak Ridge National Laboratory (R5) investigated the interaction of molten UO_2 and graphite in order to assess the capability of graphite to act as a core catcher refractory material. While MgO, Al_2O_3, and ZrO_2 all exhibted some erosion and interaction with UO_2, <u>there was no noticeable erosion of graphite and no interaction of graphite with molten UO_2</u>.

As mentioned above, an unusual and advantageous characteristic of graphite is its low coefficient of thermal expansion. This characteristic is particularly important for the liner, to minimize potential buckling. The expansion of graphite is considerably lower than virtually all other materials; the thermal expansion of copper and aluminum, for example, is about ten times that of graphite (G5).

The emissivity of graphite for thermal radiation is high, around 0.80-0.90, providing for an additional heat transfer mechanism from the bed to the surroundings (C5).

Graphite is currently used at temperatures exceeding the boiling point of UO_2 ($6000^{\circ}F$) in an air atmosphere without catching fire (V1); there is therefore no fire hazard associated with its use as a core catcher.

The strength of graphite actually increases with temperature, doubling as the temperature rises from room temperature to 2500°C--its compressive strength at room temperature is around 5100 psi (G5). Graphite's low thermal expansion coefficient combined with its good thermal conductivity and high-temperature strength give it excellent dimensional stability when heated.

The sublimation temperature of graphite is about 6700°F, above the boiling point of UO_2 (G5), so that under the worst-case temperature conditions possible, the bed should remain stable. Graphite is easily machinable, and the Union Carbide Company manufactures graphite pebbles which were in fact utilized in the experiment described in Section 5.6; these pebbles were not machined, however, but resemble cinders.

5.4 PROPERTIES OF, AND CORRELATIONS FOR POROUS MEDIA

5.4.1 Introduction

Neither exact theoretical expressions nor experimental correlations exist to describe the physical situation of present interest which involves the transient flow and heat transfer of molten matter through pebble beds. The problem is complicated by the fact that the fluid is changing phase, and simultaneously its physical property values are changing; in particular, the viscosity of the molten debris changes from some finite value to an infinite value upon freezing. Further complications exist due to the unsteady flow situation and due to the fact that the bed is unsaturated--not filled with the molten debris. Use of existing correlations which describe steady state, fully developed

flow should result in conservative (underestimated) heat transfer values, since in the transient the thermal and flow boundary layers are not fully developed. The solution to the governing relation (Laplace's Equation) for a velocity distribution in the case of gravity flow with a free surface is, to quote Ref. S2, "extremely difficult and tedious", requiring use of hodograph transformations. It is therefore necessary to mathematically model the event in terms of limiting cases and rely on conservatism in the model to conceptually validate the proposal--this is done in the next section (5.42). The properties governing flow and heat transfer in porous media which will be needed later are described below.

5.4.2 Porosity of a Pebble Bed

The porosity (ϵ), or void fraction, of a pebble bed is the fraction of the bulk volume of the material occupied by voids. For spherical packing in any one mode of stacking configuration, the bed porosity is independent of the size of the sphere; this observation is also valid for natural materials of slightly irregular shape utilized in large beds (ratio of bed diameter to particle diameter > 10) (S2). For randomly-packed, large beds the void fraction (ϵ) is about 39-41%, although nonuniformity in size would allow smaller particles to fill the spaces between larger ones, decreasing the void fraction. For modeling purposes the graphite pebbles will be considered spherical with diameter (d_p) of 0.5 inch; the bed void fraction will be taken as 0.40. An expression yielding the equivalent diameter of the interstitial

pores is given by (S2)

$$\phi = \frac{4\ dp\ \epsilon}{6\ (1-\epsilon)} \quad \text{inches,} \tag{5.4}$$

which for a dp of 0.5 inches and ϵ of 0.4 results in ϕ of 0.22 inches.

5.4.3 Laminar vs Turbulent Flow

Laminar flow of a fluid is characterized by a fixed set of streamlines; that is, a fluid element which at one instant is traversing the same path as another must continue following this path throughout its course. When a fluid flows through a porous medium, the velocity vector rapidly changes from point to point in both magnitude and direction. For steady laminar flow, the random variations in velocity must average to zero over any macroscopic volume; however, the inertial forces in the direction of flow will not average to zero and hence will only be negligible for low flow rates (C3).

For high flow rates, the inertial forces in the flow direction cannot be neglected and the flow is said to be turbulent. Generally the transition from laminar to turbulent flow is defined in terms of an effective Reynold's number:

$$Re = \frac{dp\ Go}{\mu\ (1-\epsilon)} \ , \tag{5.5}$$

where dp = bed particle diameter

Go = superficial mass flow rate

μ = fluid viscosity

ϵ = void fraction .

The transition is said to occur around a Re of about 10 (C3).

For assessing the depth of penetration it should be noted that the velocity of interest is not the actual microscopic interstitial velocity $\langle V \rangle$ but the macroscopic velocity of the leading edge, termed "superficial velocity", V_o; this is the velocity that would exist in an empty bed and is given by $V_o = \langle V \rangle \epsilon$.

5.4.4 Heat Transfer and Fluid Flow Correlations

For an assumed uniform packing with no channeling, the pressure drop across a packed bed is given by (B1)

$$Po - P_1 = \frac{4FL}{dp} \left(\tfrac{1}{2} \rho Vo^2\right) , \qquad (5.6)$$

where, Po = inlet pressure at x=o

P_1 = outlet pressure at x=L

F = friction factor

L = bed height

ρ = fluid density

Vo = superficial fluid velocity

For laminar flow (Re < 10), the Blake-Kozeny correlation gives the following relationship for the superficial velocity, Vo (B1)

$$Vo = \frac{Po - P_1}{L} \quad \frac{g_c dp^2}{150 \mu} \quad \frac{\epsilon^3}{(1-\epsilon)^2} \quad \text{Ft/hr} \qquad (5.7)$$

corresponding to a friction factor:

$$F = \frac{(1-\epsilon)^2}{\epsilon^3} \quad \frac{75}{gdp\ Go/\mu} \qquad (5.8)$$

where, Go = ρ Vo.

μ in lbm/hr-Ft

For turbulent flow (Re > 1000) the Burke-Plummer correlation describes the superficial velocity as (B1)

$$Vo = \left(\frac{2g_c}{3.5} \quad \frac{dp}{\rho} \quad \frac{\epsilon^3}{1-\epsilon} \quad \frac{(Po-P_1)}{L} \right)^{\frac{1}{2}} , \qquad (5.9)$$

corresponding to a friction factor

$$F = 0.875 \; \frac{1-\epsilon}{\epsilon^3} \; . \qquad (5.10)$$

For the transition region ($10 < Re < 1000$), Ergen adds Eqs. 5.7 and 5.9 to obtain

$$\frac{(Po-P_L)}{Go^2} \quad \frac{dp}{L} \quad \frac{\epsilon^3}{1-\epsilon} = 150 \; \frac{1-\epsilon}{dp\; Go/\mu} + 1.75. \qquad (5.11)$$

It is noted that for free gravity flow such as exists with the molten debris falling through the bed under its own weight,

$$Po-P_L = \rho \text{ debris } g/g_c \; L \quad (\text{lbF/Ft}^2). \qquad (5.12)$$

Ref. H2 investigates the cooling of a crumpled core (in a spherical configuration) heaped upon the reactor vessel bottom head, and suggests an overall bed thermal conductivity given by

$$k_b = k_c \left[1 - \frac{(1-\epsilon)\;(1-k_p/kc)}{kp/kc + (1-\epsilon)^{1/3}\;(1-kf/ks)} \right]. \qquad (5.13)$$

where, k_b = overall bed conductivity

k_c = coolant conductivity

k_p = bed particle conductivity.

Ref. Z4 provides graphical solutions for the effective stagnant bed thermal conductivity based upon the ratio of the conductivities of the bed material and the fluid occupying the interstices.

For laminar flow through a packed bed, in which heat transfer is from the bed to the coolant, Denton (D1) suggests a Nusselt number (and a resulting heat transfer coefficient) given by

$$Nu = 0.80 \quad Re^{0.7} \; Pr^{1/3} \qquad (5.14)$$

$$\text{or } \frac{hd_p}{k} = 0.80 \left(\frac{d_pG_o}{\mu}\right)^{0.7} \left(\frac{C_p\mu}{k}\right)^{1/3} \tag{5.14a}$$

where all properties are those of the moving fluid.

An equivalent expression for turbulent flow is suggested in Ref. E1a as,

$$h = 0.43\ G_oC_pR_e^{0.3}Pr^{0.66} \tag{5.15}$$

$$\text{or } h = 0.43\ G_oC_p\left(\frac{d_pG_o}{\mu}\right)^{0.7}\left(\frac{C_p\mu}{k}\right)^{1/3} \tag{5.15a}$$

5.5 PERFORMANCE OF THE GRAPHITE BED

A necessary, though not sufficient, criterion for the initial quenching and freezing of the debris is that the interface temperature between the melt and the graphite pebbles be less than the melting point of the debris. Another necessary requirement is that there be sufficient graphite mass enveloped to adiabatically freeze the debris. Both of these criteria are shown to be readily satisfied. By determining the superficial flow velocity (V_o) of the leading edge of the molten debris in passing through the pebble bed, and by resolving the time (t) required for this edge to freeze, one can obtain an estimate of the depth of penetration into the bed prior to flow stoppage.

5.5.1 Interface Temperature Assuming Semi-Infinite Bodies

At the instant of contact the graphite pebbles appear as a semi-infinite body to the melt, and vice-versa (G6); it is shown in Section 5.5.4 that the time required to freeze is so small that this is a valid approximation throughout the freezing process. The interface temperature (T_i) for this situation is given by (M4)

$$\frac{T_i - T_g}{T_d - T_i} = \left(\frac{(\rho k C_p)_d}{(\rho k C_p)_g}\right)^{1/2} , \qquad (5.16)$$

which for the following properties (seeAppendix A) and initial temperatures:

Debris (d): $T_d = 6000\ ^oF$
$\rho = 473.8\ lbm/ft^3$
$k = 1.73\ BTU/hr\text{-}ft\text{-}^oF$
$C_p = 0.09\ BTU/lbm\text{-}^oF$

Graphite (g): $T_g = 100\ ^oF$
$\rho = 110\ lbm/ft^3$
$k = 100\ BTU/hr\text{-}ft\text{-}^oF$
$C_p = 0.3\ BTU/lbm\text{-}^oF$

gives rise to:

$$\frac{T_i - T_g}{T_d - T_i} = 0.1494$$

and, $T_i = 866.9\ ^oF$. It is therefore seen that this interface temperature is well below the freezing point of UO_2 ($T_m = 5072\ ^oF$).

5.5.2 <u>Mass of Graphite Required for Adiabatic Equilibrium</u>

It was shown above that the initial quenching and freezing process conservatively (assuming 250 tons of debris at T_{v,UO_2}) requires the removal of about 9.57×10^7 BTU. For steady state equilibrium at the freezing point of UO_2, the mass of graphite (g) required is:

$$(MC_p\Delta T)_g = 9.57 \times 10^7 \text{ BTU} \qquad (5.17)$$

$$M_g = \frac{9.57 \times 10^7}{0.3(5072-100)}$$

$$= 64{,}159.3 \text{ lbm.}$$

A 7.5 foot deep bed covering the core catcher deck area of 1774.25 square feet with a void fraction of 0.4 contains

$$M_g = 7.5 \times 1774.25 \times 0.6 \times \rho_g \qquad (5.18)$$

and the mass of graphite present is 878,253.75 lbm. The bed therefore contains a factor of 12 excess graphite to perform the initial quenching and freezing; conversely, an envelopment of only about 6.6 inches of bed is required even without other energy loss mechanisms.

5.5.3 Superficial Velocity Through the Bed

A combination of Eqs. 5.7 and 5.12 gives the superficial flow velocity for laminar flow as

$$V_o = \rho_d g \frac{d_p^2}{150\mu} \frac{\epsilon^3}{(1-\epsilon)^2} , \tag{5.19}$$

which, for the debris and bed properties used previously gives

$$V_0 = 473.8 \frac{(1/24)^2}{150(2.42)} \frac{(0.4)^3}{(0.6)^2} (4.16 \times 10^8)$$
$$= 47.2 \text{ ft/sec.}$$

However, the Reynold's number corresponding to this flow is given by Eq. 5.5 as

$$Re = \frac{d_p G_o}{\mu(1-\epsilon)} \tag{5.5}$$
$$= 2.3 \times 10^6, \gg 10, \text{ the transition value,}$$

implying the Burke-Plummer Correlation (Eq. 5.9) should be used instead.

Combining Eqs. 5.9 and 5.12, one obtains for turbulent flow,

$$V_0 = \left(\frac{2}{3.5} d_p g \frac{\epsilon^3}{1-\epsilon}\right)^{1/2} , \tag{5.20}$$

which yields $V_0 = 0.286$ ft/sec and a Reynold's number of 1.4×10^4. Both correlations therefore indicate a definite turbulent flow regime.

5.5.4 Time for Debris Freezing

The interstitial pore channel diameter was determined from

Eq. 5.4 to be 0.22 inches or 0.0185 feet. An estimate of the time to freeze may be obtained by treating the pores as cylindrical rods of the same diameter. The most slowly decaying mode in a rod of cooling UO_2 is characterized by (P2):

$$\frac{T(t)}{T(o)} = e^{-\lambda t} , \qquad (5.21)$$

where $\lambda = \left(\frac{k}{\rho C_p}\right) \left(\frac{\nu_o}{r}\right)^2$

and $\nu_o =$ 2.405... (first root of J_o Bessel Function).

Thus, the maximum cooldown time from 6000 ^oF to 5072 ^oF when internal conduction is the limiting step, is, from Eq. 5.21,

$$t = \frac{\rho C_p}{k} \left(\frac{r}{\nu_o}\right)^2 \ln\left(\frac{6000}{5072}\right)$$

$$= 0.882 \text{ seconds.}$$

An alternate estimate of the time for the debris to cool from the UO_2 boiling point to the UO_2 freezing point can be obtained by considering the limiting case of slug flow through the graphite bed. For an incremental element of debris of height dx progressing downward in the x direction, the incremental rate of heat transfer is given by

$$dq \text{ (BTU/hr)} = h dA \Delta T . \qquad (5.23)$$

where h is given by the Nusselt number for slug flow with constant wall temperature (R2):

$$\frac{hd}{k} = 5.75 \qquad (5.24)$$

or, $h = \frac{5.75k}{2r}$,

and where,

$$dA = 2\pi r\, dx \qquad (5.25)$$

$$\Delta T = T(x)_d - T_g . \qquad (5.26)$$

The energy removed from the debris is also given by

$$dq = \rho C_p d(\text{Volume}) \frac{dT_d}{dt} \tag{5.27}$$

where $d(\text{Volume}) = \pi r^2 dx.$ (5.28)

Equating Eqs. 5.23 and 5.27, one obtains

$$5.75k(T(x)_d - T_g) = r^2 \rho C_p \frac{dT_d}{dt} \tag{5.29}$$

and integrating Eq. 5.29 between T_{v,UO_2} and T_{m,UO_2},

$$\frac{T_m - T_g}{T_v - T_g} = \exp(-5.75kt/r^2 \rho C_p) \tag{5.30}$$

$$\text{or,} \quad t = (r^2 \rho C_p/5.75k) \ln \frac{T_v - T_g}{T_m - T_g} . \tag{5.31}$$

For debris and bed parameters used above,

$$t = \left((0.0185)^2(473.8)(0.09)/(5.75)(1.73)\right) \ln(5900/4972)$$
$$= 0.904 \text{ seconds.}$$

The above models result in a very conservative estimate of the time required to freeze the debris front, assuming in both cases a purely conductive heat transfer process. It is noted that these methods yield nearly equivalent results, the differences being due to the assumption of a J_o Bessel Function temperature distribution in Eq. 5.22 and a parabolic temperature distribution in Eq. 5.31. A final model, due to Mikic (M2), includes the volumetric heat production (q''') from fission product decay and provides a more realistic estimate of the time to freeze. The molten debris is considered to fill the void spaces in the bed so that a heat balance on an element of melt moving through the bed yields (modeled as if the pebbles are moving past the melt with superficial velocity V_o) :

$$\epsilon q''' x = Vo(\rho C_p)_2(T-T_{o,2})(1-\epsilon) + (\rho C_p)_1\epsilon \frac{dT_1}{dt} \Delta x \qquad (5.32)$$

or

$$\epsilon q''' = V_o(\rho C_p)_2 \frac{d\theta}{dx} (1-\epsilon) + (\rho C_p)\epsilon \frac{dT_1}{dt} \qquad (5.33)$$

where subscript 2 refers to graphite properties,
subscript 1 refers to debris properties,
$dx = V_o \tau$
τ = time for a pebble initially at $T_{o,2}$ (with an instantaneous boundary change to $T_{o,1}$) to arrive at an average temperature of $0.9T_{o,1}$.
$\theta = T(t)_2 - T_{o,1}$

Thus, Eq. 5.33 results in

$$\epsilon q''' = (\rho C_p)_2 \frac{\theta}{\tau} (1-\epsilon) + (\rho C_p)_1\epsilon \frac{d\theta}{dt} \quad . \qquad (5.34)$$

The particular solution to Eq. 5.34 is

$$\theta_p = \frac{q'''\epsilon}{1-\epsilon} \frac{\tau}{(\rho C_p)_2} \quad , \qquad (5.35)$$

and the homogeneous solution is

$$\theta_h = C \exp\left[- \frac{(\rho C_p)_2}{(\rho C_p)_1} \frac{(1-\epsilon)}{\epsilon} \frac{t}{\tau}\right] \qquad (5.36)$$

for the initial condition

$$\theta = T - T_{o,2} = \theta_o \text{ at } t = 0, \qquad (5.37)$$
$$\theta_o = T_{o,1} - T_{o,2} = 5900 \text{ }^oF.$$

The complete solution to Eq. 5.34, given by the sum of the homogeneous (Eq. 5.36) and particular (Eq. 5.35) solutions, with the coefficient, C, determined from Eq. 5.37, is:

$$\theta_s = \theta_o \exp\left[- \frac{(\rho C_p)_2}{(\rho C_p)_1} \frac{1-\epsilon}{\epsilon} \frac{t}{\tau}\right] + \frac{q'''}{(\rho C_p)_2} \frac{\epsilon}{1-\epsilon} 1-\exp\left[\frac{(\rho C_p)_2}{(\rho C_p)_1} \frac{1-\epsilon}{\epsilon} \frac{t}{\tau}\right] \qquad (5.38)$$

where $\theta_s = T_{m,1} - T_{o,2} = 4972 \text{ }^oF$
$\theta_o = T_{o,1} - T_{o,2} = 5900 \text{ }^oF.$

The value for τ is determined from solutions plotted in Ref. M4 for spherical geometry pebbles as,

$$\left(\frac{k}{\rho C_p}\right)_2 \frac{\tau}{r_2{}^2} = 0.28, \tag{5.39}$$

and thus $\tau = 0.23$ seconds. The volumetric heat generation rate is determined from the decay heat rate at 1 hour and the volume of the debris as

$$\begin{aligned} q'''\frac{\text{BTU}}{\text{hr-ft}^3} &= \frac{q(\text{BTU/hr})}{\text{Volume}(\text{ft}^3)} \\ &= \frac{1.8 \times 10^8 \text{ BTU/hr}}{250\text{ton} \times 2000\frac{\text{lb}}{\text{ton}} \times \frac{1}{473.8}\frac{\text{ft}^3}{\text{lb}}} \\ &= 1.75 \times 10^5 \text{ BTU/hr-ft}^3 \end{aligned} \tag{5.40}$$

The ratio $(\rho C_p)_2/(\rho C_p)_1$ is determined from the previously quoted properties of the debris and graphite as

$$\frac{(\rho C_p)_2}{(\rho C_p)_1} = \frac{(110)(0.30)}{(473.8)(0.09)} = 0.774. \tag{5.41}$$

The ratio $(1-\epsilon)/\epsilon$ for ϵ of 0.40 is

$$\frac{1-\epsilon}{\epsilon} = 1.5 \tag{5.42}$$

Inserting the results of Eqs. 5.39, 5.40, 5.41, and 5.42 into Eq. 5.38, one obtains

$$4972 = 5900\exp\left[-\frac{(0.774)(1.5)}{0.23}t\right] + \frac{(170,000)(0.23)}{(1.5)(33)}\left[1 - \exp\left(-\frac{(0.774)(1.5)}{0.23}t\right)\right] \tag{5.43}$$

and t = 0.040 seconds.

This value for the quench time is a factor of 20 less than the ultra-conservative estimates represented by Eqs. 5.22 and 5.31.

5.5.5 Penetration Depth

Table 5.4 summarizes the velocities, time to freeze, and penetration depths for the various models used in Sections 5.5.3 and 5.5.4.

Table 5.4

SUMMARY OF PREDICTED VELOCITIES AND TIMES TO FREEZE WITH RESULTING PENETRATION DEPTHS

Velocity Model	Velocity	Comment
Laminar	(47.2 ft/sec)	The resulting Reynold's Number invalidates use of this correlation. Upper limit of use is Re=10, while this velocity yields $Re=2.3x10^6$.
Turbulent	0.286 ft/sec	$Re=1.4x10^4$ indicates definite turbulent regime; however, may underestimate actual velocity (G6, H4, M2).

Heat Transfer Model	Freeze Time	Penetr. Depth	Comment
Rod of UO_2	0.882 sec	3.02 in.	Conductive mode of heat transfer decay assumed. Conservative.
Slug Flow	0.904 sec	3.10 in.	Assumes laminar slug flow, conductive heat transfer, and again results in a conservative estimation of the freeze time.
Mikic Heat Balance	0.040 sec	0.14 in.	Includes effect of decay heat. Most realistic model.
Static Envelopment	---------	6.60 in.	Adiabatic heat balance.

5.5.6 Post-Freeze Penetration

The previous section indicates that the debris will freeze almost immediatley upon contacting the graphite pebble bed, form-

ing its own "UO_2 lined-crucible." If the fission product decay heat is not removed from the frozen mass, the debris and adjacent graphite pebbles will gradually heat up, and the meltdown products will remelt and slump further into the graphite; this slumping/freezing progression will continue until active cooling is initiated or until the debris reaches the catcher bottom. An estimate of this rate of penetration following the initial freeze can be obtained through an adiabatic envelopment model by equating the fission product decay heat rate to the rate of heat absorbed by the graphite bed plus the rate of melting of the debris. Since it is questionable how much debris must be melted to allow downward slumping, this heat of fusion will be neglected to provide a conservative estimate of the penetration rate, V_p. Thus,

$$V_p(\rho C_p A \Delta T \epsilon)_{graphite} = q_{decay\ heat} \tag{5.44}$$

and,

$$V_p = \frac{1.8 \times 10^8}{(110)(0.4)(1774.25)(5072-100)}$$
$$= 0.464 \text{ ft/hr.}$$

To traverse the bed depth of 7.5 feet requires

$$t = \frac{7.5}{0.464}$$
$$= 16.2 \text{ hours,}$$

and it is seen that the time span available to initiate active cooling is well within reason.

The waiting-time available for active cooling may be limited by another consideration--the time required to heat the compartment bulkheads and deck to a temperature where the steel loses its strength. The entire graphite bed could be adiabatically

heated from 100 ^{o}F (assumed initial temperature) to 1000 ^{o}F (assumed steel temperature limit) in about 1.32 hours at the decay heat rate of 1.8×10^{8} BTU/hr. This is a conservative indication of the time to initiate active cooling since the effective bed thermal conductivity was shown in Section 5.2 to be less than many refractory materials (0.25 BTU/hr-ft-^{o}F), and thus the bed would actually insulate the compartment bulkheads and deck. Additionally, the process (even with no external cooling) would not be adiabatic, some heat being removed by radiation from the upper surfaces.

5.5.7 Post-Freeze Bottom Cooling

Flooding the graphite bed from below via installed drain lines or via drainage from installed reactor systems is the preferred cooling method, since no additional headers (and subsequent containment penetrations) or design changes are required, contrary to the above-bed spray method of cooling.

Steam at atmospheric pressure has a specific volume of 26.8 Ft^{3}/lbm and a heat of vaporization of 970.4 BTU/lbm (K2). If all of the decay heat energy available goes to boil water, about 1.86×10^{5} lbm/hr, or 1400 ft^{3} steam/sec, must be vaporized to remove the one-hour decay heat rate of 1.8×10^{8} BTU/hr. The total void volume available in the bed for steam and water is 5322.75 cubic feet. Allowing 50% of this space for steam and 50% for water, an amount of steam equivalent to about 52.6% of the free volume will be vaporized each second. If the debris solidifies over an area of 490 square feet (a 25 foot diameter

circular disc), there remains 713.7 square feet of voided area available for venting the steam. At 1400 ft^3/sec the resulting steam velocity (superficial) is only about 1.96 ft/sec, a gentle steaming rate. Conversely, to prevent fluidizing the bed, it was shown in Section 5.2 that a steam velocity of 375.8 ft/sec must not be exceeded; to vent the required 1400 ft^3/sec at this limiting velocity would require only <u>3.73 square feet</u> of open flow area. This method of cooling therefore appears to be theoretically feasible, and the top surface, above-bed spray may not be required to cool the debris.

5.6 EXPERIMENTAL VERIFICATION

A concern with utilizing lightweight graphite pebbles to stop the flow of the heavy molten debris was that the melt would rapidly float the immersed graphite, thereby avoiding sufficient contact for effective heat transfer, and hence progress to the vessel bottom. Although the analysis in Eqs. 5.32-5.43 would appear to rule out this possibility, the potential consequences were considered sufficiently important to warrant experimental investigation.

Since the primary purpose of the experiment was to investigate the buoyancy problem, a model for UO_2 was chosen based primarily upon density similarity. An obvious choice was lead, with a density essentially identical to that of UO_2. Lead is also a reasonable substitute for UO_2 as a heat transfer model. In particular, it was shown in Section 5.5.1 that the transient heat transfer process, especially during the rapid quench, de-

pends upon the value of $\sqrt{\rho K C_p}$ for the molten material. The ratio of this parameter for lead to that for UO_2 is

$$\left(\frac{(\rho K C_p)_{pb}}{(\rho K C_p)_{UO_2}}\right)^{1/2} = \left(\frac{(658)\ (9.3)\ (0.038)}{(600)\ (2.6)\ (0.10)}\right)^{1/2} = 1.49 \qquad (5.45)$$

For future investigations in this area, one could match this quantity exactly by utilizing a lead alloy (lead and Bi, Cd, In, Sn, etc.) to slightly reduce the alloy's thermal conductivity. Another comparison of the validity of a lead model may be seen by comparing the turbulent heat transfer coefficients from Eq. 5.15,

$$h \propto \rho C_p \left(\rho/\mu\right)^{0.3} \left(\frac{\mu C_p}{k}\right)^{0.66} \qquad (5.46)$$

or

$$h \propto \rho^{1.3}\ C_p^{1.66}\ k^{-0.66}\ \mu^{0.36}$$

Again forming a ratio

$$\frac{h_{Pb}}{h_{UO_2}} = \left(\frac{658}{600}\right)^{1.3} \left(\frac{0.038}{0.10}\right)^{1.66} \left(\frac{9.3}{2.6}\right)^{-0.66} \left(\frac{5.86}{3.63}\right)^{0.36}$$

$$= (1.127) \times (0.201) \times (0.431) \times (1.188)$$

$$= 0.116$$

As can be seen the large difference in heat capacity is responsible for the discrepancy in h. However, another comparison of the heat transfer coefficients of lead and uranium dioxide may be obtained from the laminar, slug flow analysis in which

$$Nu = \frac{hD}{k} = \text{constant.} \qquad (5.47)$$

For this case,

$$\frac{h_{Pb}}{h_{UO_2}} = \frac{k_{UO_2}}{k_{Pb}} = 3.6$$

Yet another comparison is afforded by Eq. 5.14, describing the laminar flow Nusselt number for packed-bed flow. Here,

$$h \propto k^{0.667} \mu^{-0.367} Cp^{0.333} \rho^{0.7}, \qquad (5.48)$$

such that $h_{Pb}/h_{UO_2} = 1.53$. The true relationship for the heat transfer coefficient comparison probably lies somewhere between the values predicted by Eqs. 5.46-5.48.

The thermal diffusivity ($k/\rho Cp$) of lead is 8.6 times as large as that of UO_2, again primarily because of the discrepancies in Cp and k. This difference results in the lead cooling faster than UO_2.

The experiment to ensure that the high density molten debris would not float the lighter graphite pebbles consisted of pouring 12 pounds of molten lead at $740^{\circ}F$ into a 10 inch diameter container filled with graphite pebbles of a nearly spherical shape. The pebbles had an average diameter of about 0.25 inch. Upon being poured into the container, the molten lead did not push aside or float the pebbles but, in fact, froze and plugged the flow almost instantly upon contacting the graphite (which was initially at a room temperature of about $80^{\circ}F$). The average penetration depth of the molten lead was about 1.75 inches. There were no noticeable interactions with the graphite, and no external means of cooling the container were provided.

All the heat transfer coefficient correlations previously considered indicate that for a hypothetical melt with an extremely small value of the thermal conductivity, k, a similarly small heat transfer coefficient is obtained, and the molten

material should not transfer heat rapidly enough to freeze prior to reaching the bed bottom. Experimental verification of this was obtained by pouring molten wax ($k = 0.14$ BTU/hr-Ft-$^{\circ}$F, compared to 9.3 for Pb) through an identical graphite bed as used in the molten lead experiments; while the lead froze almost instantly, reaching an average depth of 1.75 inches, the wax passed through the bed, pooling at the container bottom. Conversely, if the bed material had possessed an extremely low thermal conductivity, the melt again could not freeze: in the limit of zero conductivity, there would be no heat transfer. Thus the choice of a high conductivity bed material such as graphite is a vital one.

5.7 SUMMARY

A new core catcher concept, utilizing graphite in a pebble-bed configuration, has been described. Although an exact analytical solution to the penetration depth has not been obtained, several conservative analytical models were described, each of which verifies the concept of freezing the molten meltdown products with a depth far less than that proposed for use on the barge-mounted plant. Long-term cooling of the bed may be provided after the debris is frozen by adding suction supply headers and spray headers to the existing piping of the Containment Spray System, and then utilizing that system's pumps and heat exchangers, and ultimately the ocean, as the heat sink. This cooling method can be augmented or completely supplanted by back-flooding the bed, providing steam and water as a coolant

for the debris. Over an hour is available (conservatively) before the bed heats to 1000°F, causing the compartment's steel bulkheads to lose structural strength. If this limit is not reached, then over 16 hours would pass before the re-molten debris slumped completely through the bed. In either case sufficient time exists to initiate active cooling.

Chapter 6

SUMMARY AND RECOMMENDATIONS

6.1 SUMMARY

Increasing energy demands, coupled with environmental restrictions upon power plant siting, and potential economies in construction, have led to plans for offshore siting of central power stations. The presence of open ocean rather than earth underneath the plant places increased emphasis on the provision of added insurance for retention of core meltdown products in the event, however unlikely, of a gross reactor core meltthrough. This thesis has evaluated existing proposals designed to mitigate the effects of a meltdown accident and has found none to be totally satisfactory for use on the presently envisioned barge-mounted power plant proposed by Offshore Power Systems. A new concept, the graphite pebble bed, was then presented and was shown to circumvent many of the problems associated with the other designs.

In order to evaluate the compatibility of various core catcher proposals with the presently envisioned barge-mounted plant, potential interactions of a core catcher with other shipboard systems were analyzed, and the area and volume available for the location of the device were established. This plant (a <u>standard</u> Westinghouse Pressurized Water Reactor and Turbine-Generator system) contains over 100,000 cubic feet of water which could conceivably drain into a core catcher during the accident, and the Containment Spray System has the ability

to reject heat at the rate of 5.6×10^7 BTU/hr to the ocean. The structural framework of the barge forms a compartment directly under the reactor vessel, and below the Containment Base Plate, of dimensions 47' x 37 3/4' x 7 1/2'; this area is further divided into four spaces by 7 1/2' deep transverse girders. The barge is designed to float at a draft of 32 feet with less than 1° pitch and 0.7° roll, placing upper limits on the acceptable weight and moment of a proposed core catcher.

An analytical evaluation of the meltdown sequence, developing the quantitative aspects of the accident needed to evaluate the capability of a proposed core catcher has been presented. In particular, the major events of the accident and the expected times of occurrence were determined to be: Blowdown (5-45 sec); Core Heatup/Collapse (10-60 min); Grid Support Meltthrough (30 min); Reactor Vessel Meltthrough (20-120 min); Containment Base Plate Meltthrough (22 min). Neglecting the time required to meltthrough the Containment Base Plate and ignoring the loss of volatile fission products from the molten debris, it was concluded that an extremely conservative estimate of 250 tons of molten debris at 6000 $^\circ$F, emitting decay heat at a rate of 1.8 $\times 10^8$ BTU/hr, would be considered to enter the proposed core catcher 1 hour after blowdown.

The analytical description of the meltdown accident and the resulting development of a worst-case heat load on the core catcher, along with the volume and hydrostatic considerations generated from the description of the barge plant, provided data for a detailed evaluation of existing core catcher proposals.

All of the proposals were found to be at least partially deficient, primarily due to weight/volume requirements, high cost, excessive complexity, metallurgical and thermophysical uncertainties, and potential steam explosion hazards.

A new concept employing a graphite pebble bed--whose voids provide for water drainage to minimize the possibility of steam explosions, which is configurationally compatible with the existing barge compartmentation and requires no additional crucible for support, whose open lattice-work allows cooling water to completely surround the frozen debris, and which affords impact protection to the barge bottom--was introduced. Graphite as both bed material and bottom liner was shown to be superior to other candidate materials due to its relatively low cost, high heat capacity (per pound <u>and</u> per unit volume), high contact thermal conductivity for the particles in the bed (and low conductivity for the liner), and high strength. The total weight of the catcher was shown to be 408.15 long tons, resulting in an additional barge draft of a tolerable 1.05 inches. Mathematical models describing the entry of the molten material into a bed of 0.5 inch diameter graphite pellets resulted in a penetration depth before initial freezing of only a few inches. Without external cooling the bed and debris would heat up and the debris would gradually slump through the bed, potentially overheating the barge bulkhead structure or reaching the bottom of the bed in from 1 to 16 hours, allowing sufficient time to initiate active water cooling: there is little danger of steam explosions when water contacts the now-solidified mass.

Finally, experimental results were described which conceptually verify the proposal, in which molten lead was poured onto a graphite pebble bed to simulate the quick freeze of a batch of UO_2.

6.2 RECOMMENDATIONS FOR FUTURE WORK

No effort has been made in this thesis to optimize the pebble-bed design or to extensively evaluate the means of post-freeze cooling of the debris. Future work in this area should therefore have as specific objectives: (1) reduction of the bed thickness, optimizing the overall weight and cost of the design; (2) investigation of the use of other, cheaper materials (such as cobblestones) as a base for the catcher, employing the graphite bed only for the top layer; (3) elimination of the graphite liner, and; (4) more detailed evaluation of the post-freeze cooling method, with a major goal being elimination of the proposed overhead spray.

To help investigate reducing the bed thickness and replacing some of the graphite with a cheaper material, it is recommended that an improved mathematical model, which evaluates the effect of debris property changes, especially viscosity, as the debris nears freezing, be developed.

Additional meltdown simulations on a larger scale and utilizing a material which more closely resembles molten UO_2 is recommended. A brief evaluation of the validity of utilizing molten lead to simulate molten UO_2 was presented in Chapter 5; it was shown that not all the necessary similitude for a heat

transfer analysis is provided by the lead, and it is suggested that a lead alloy employing tin, bismuth, or indium might be used to better simulate the UO_2 properties.

To investigate elimination of the graphite liner and optimization of the post-freeze cooling method, additional calculations, and more importantly, experimental simulations of this phase of the meltdown are strongly recommended. The calculations and simulations of the post-freeze period should have as their objectives: (1) the determination of the heat removal rate from the debris by bed conduction; (2) assessment of bed and the surrounding compartment steel heat-up rates if cooling is delayed; (3) measurement of the slumping rate of the solidified mass; (4) investigation of the effects on bed stability of steam venting and water chugging, and; (5) evaluation of the cooling of the suspended debris from below in the two-phase flow regime.

6.3 CONCLUDING REMARKS

A reactor core meltdown at sea for the size reactors currently being considered for offshore power generation has potentially serious consequences, and even despite the demonstrably low probability of occurrence, the added expense of an additional margin of safety to prevent the uncontrolled release of fission products to the environment may be justifiable. Recent reports indicate a potential proliferation of the offshore plants, now including Europe and the west coast of the United States (N7, S6); Offshore Power Systems now anticipates proposals for six more

units for the east coast in about two months in addition to two units already committed for PSEG of New Jersey (D5). Thus, the question as to whether added meltthrough protection is justifiable will undoubtedly soon be raised, debated, and decided. If a core catcher is mandated, the graphite bed proposed in this thesis is relatively inexpensive, maintenance-free, easy to install, and conceptually successful in stopping and retaining the meltdown products.

Finally, special attention is called to the German pebble-bed reactor (AVR). The experience obtained on this concept has obvious application to the design of a pebble-bed core catcher, and this project is a suggested source for acquisition of pertinent data on the operating thermal characteristics of graphite pebble beds.

Appendix A

COLLECTION OF PERTINENT DATA

Evaluation of the core meltdown accident requires an input of many physical properties and parameters. Widely varying values have been utilized in different studies, resulting in significantly dissimilar conclusions.

The data considered germane to meltdown evaluation are reviewed below, indicating the source of the proposed value and, identified by an asterisk *, the value selected for use in this report. The nomenclature used is identified in Appendix B.

UO_2 Properties

Property	Value	Ref
T_m	5000 °F	M3
	*5072	H1, Z1
	5080	T2
	5100	E3, R1
T_v	5732	M3
	5824	T2
	5972	Z1
	*6000	E1
k, solid	1.2 BTU/hr-ft-°F	H2, Z1
	*1.85	E3, M3
k, liquid	*2.6	E3, M3
ρ	586 lbm/ft^3	Z1
	550	M3
	643	D2
	*600	
C_p	0.06 BTU/lbm-°F	D2
	0.09	R1
	0.123	M3
	*0.10	
μ	*3.63 lbm/hr-ft	M3
λ_f	121.3 BTU/lbm	M3
	*108.0	Z1, R1
β	5.83 x 10^{-5} °F^{-1}	M3

Clad Properties-Zircaloy

Property	Value	Ref
T_m	3000 °F	M3
	*3360	E3
	3365	W2
	3722	W4

Property	Value	Ref
	*405.8 lbm/ft^3	W2
Heat of Reaction	*280.0 BTU/lbm	W2
C_p	0.086 BTU/lbm-^oF	M3
k	10 BTU/hr-ft-^oF	E3

Molten Debris Properties

Property	Value ^oF	Ref
$T_m(Mg + UO_2)$	3900	E3, P1
$T_m(UO_2 + ZrO_2)$	3900	E3, P1
$T_m(UO_2 + Fe)$	2800	M3
T_m(debris)	*5072	UO_2 value
ρ	473.8 lbm/ft^3	F2
μ	2.42 lbm/hr-ft	F2
k	1.73 BTU/hr-ft-^oF	F2
C_p	0.09 BTU/lbm-^oF	F2
β	1.26×10^{-5} $^oF^{-1}$	F2

Graphite Properties

Property	Value ^oF	Ref
T_v	6020	G2
	*6512	G5
	7460	C5
	6692	C1
C_p	0.48 BTU/lbm-^oF	C5
ρ	140.5 lbm/ft^3	C1
	*110.0	G5
k	*100 BTU/hr-ft-^oF	G4
	64-109	C1
Compressive Strength	5076 lbf/in^2	B2
	8300	G5
β	25×10^{-7} $^oF^{-1}$	G5

Iron Properties

Property	Value ^oF	Ref
T_m	2795	M3
T_v	5432	M3
λ_f	119.2 BTU/lbm	M3
λ_v	2727	M3
C_p	0.17 BTU/lbm-^oF	M3
	*0.12	H2
ρ	490 lbm/ft^3	H2, M3
k	25 BTU/hr-ft-^oF	M3

Carbon Steel Properties

Property	Value ^oF	Ref
T_m	2600	T2
T_V	5400	T2

Property	Value	Ref
k	25.5 BTU/hr-ft-°F	M3
λ_F	119.2 BTU/lbm	M3
C_p	0.12 BTU/lbm-°F	R1
ρ	490 lbm/ft^3	M3

Lead Properties

Property	Value	Ref
T_m	621.3 °F	D2
T_v	3158.6	D2
λ_F	10.6 BTU/lbm	D2
ρ (700 °F)	658 lbm/ft^3	D2, R2
k (700 °F)	9.3 BTU/hr-ft-°F	D2, R2
μ (700 °F)	5.86 lbm/hr-ft	D2, R2
C_p (700 °F)	0.038 BTU/lbm-°F	D2, R2
ϵ (700 °F)	0.63	C1

Concrete Properties

Property	Value	Ref
k	0.64 BTU/hr-ft-°F	M3
C_p	*0.21 BTU/lbm-°F	M3
	0.27	H2
ρ	140 lbm/ft^3	H2, M3

Core and Plant Properties

Property	Value	Ref
Total core C_p	4.07x10^4 BTU/°F	M3
Fuel rod temp @ 25 sec	928 °F	M3
Reactor vessel head area	350 ft^2	E3
volume	842.2 ft^3	M3
thickness	14 in	E3
Total primary coolant	12,600 ft^3	W2
	562,250 lbm	W2
Blowdown mass release	513,900 lbm	O1
Blowdown energy release	3.21x10^6 BTU	O1
Containment diameter	120 ft	O1
height	162 ft	O1
volume	1.25x10^6 ft^3	O1
UO_2 weight	218,000 lbm	W1
Zircaloy weight	44,500 lbm	W1
Stainless from reactor	125,000 lbm	W1
Thermal power capacity	3411 MWt	O1

Appendix B

NOMENCLATURE

α	thermal diffusivity, Ft^2/hr
β	coefficient of volume expansion, $^{\circ}F^{-1}$
Cp	specific heat, BTU/lbm-$^{\circ}F$
d	system characteristic diameter, Ft
D	diffusion coefficient, Ft/hr
e	total emissivity, dimensionless
ϵ	packed bed void fraction, dimensionless
g	gravitational acceleration, Ft/hr^2
g_c	constant, lbm Ft/lbf-hr^2
Gr	Grashof Number $(=\frac{g \beta \Delta T L^3 \rho^2}{\mu^2})$, dimensionless
h	heat transfer coefficient, BTU/hr-Ft^2-$^{\circ}F$
k	thermal conductivity, BTU/hr-Ft-$^{\circ}F$
L	system characteristic length, Ft
λ_f	heat of fusion, BTU/lbm
λ_v	heat of vaporization, BTU/lbm
μ	dynamic viscosity, lbm/hr-Ft
Nu	Nusselt Number $(=\frac{hd}{k})$, dimensionless
$\dot{m}$	mass Flow rate, lbm/hr
Pr	Prandtl Number $(=\frac{Cp\mu}{k})$, dimensionless
Q	total system heat, BTU
q	total system heat rate, BTU/hr
q"	total system heat rate per unit area, BTU/Ft^2-hr
q"'	total system heat rate per unit volume, BTU/Ft^3-hr
Ray	Rayleigh Number $(=\frac{g \beta \Delta T L^3 \rho^2 Cp}{k\mu})$, dimensionless

Appendix B (con't)

ρ	density, lbm/Ft^3
Sc	Schmidt Number (= $\frac{\mu}{\rho D}$), dimensionless
t	time, hr
τ	time constant, hr^{-1}
Tm	melting (freezing) point, ^oF
Tv	boiling (condensing) point, ^oF
u,v,w	velocity in x,y,z direction, Ft/sec
V	system volume, Ft^3

Appendix C

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